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Proceedings of the Seminar on

LEAK-BEFORE-BREAK: Further Developments in Regulatory Policies and Supporting Research

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Held at
Taipei, Taiwan
May 11-12, 1989

Edited by
G. M. Wilkowski, Battelle
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Sponsored by
Chung-Hwa Nuclear Society, and
Taiwan Section of American Nuclear Society

U.S. Nuclear Regulatory Commission
Office of Nuclear Regulatory Research
Washington, D.C. 20555

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PREVIOUS DOCUMENTS IN THE SERIES

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Leak Before Break: International Policies and Supporting Research.
Proceedings of a seminar held at Columbus, Ohio on October 28-30, 1985,
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Leak-Before-Break: Progress in Regulatory Policies and Supporting Research.
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0092, published in March 1987.

ABSTRACT

The fourth in a series of international Leak-Before-Break (LBB) Seminars supported in part by the U.S. Nuclear Regulatory Commission was held at the National Central Library in Taipei, Taiwan on May 11 and 12, 1989. The seminar updated the international policies and supporting research on LBB. Attendees included representatives from regulatory agencies, electric utilities, nuclear power plant fabricators, research organizations, and academic institutions.

Regulatory policy was the subject of presentations by Mr. G. Arlotto (U.S. NRC, U.S.A.), Dr. B. Jarman (AECB, Canada), Dr. P. Milella (ENEA-DISP, Italy), Dr. C. Faigy (EDF/Septen, France), and Dr. K. Takumi (NUPEC, Japan). A paper by Mr. K. Wichman and Mr. S. Lee of the U.S. NRC Office of Nuclear Reactor Regulation is included as background material to these proceedings; it discusses the history and status of LBB applications in U.S. nuclear power plants. In addition, several papers on the supporting research programs described regulatory policy or industry standards for flaw evaluations, e.g., the ASME Section XI code procedures. Supporting research programs were reviewed on the first and second day by several participants from Taiwan, U.S., Japan, Canada, Italy, and France.

ACKNOWLEDGMENTS

This was the fourth international seminar held on the subject of Leak-Before-Break (LBB), supported in part by the U.S. Nuclear Regulatory Commission (NRC). This seminar was held at the National Central Library in Taipei, Taiwan on May 11 and 12, 1989.

The major efforts in organizing and supporting this conference were made by the Chung-Hwa Nuclear Society, the Taiwan section of the American Nuclear Society, and the Taiwan Power Company. Mr. S.-C. Cheng and Mr. K.-S. Chao were primarily responsible for this seminar. We owe a great deal of thanks to the many staff members of the Taiwan Power Company for their organizational efforts.

These conference proceedings were assembled and organized by Mr. K.-S. Chao and Dr. G. Wilkowski. We thank those who have contributed their presentations to these proceedings. Finally, thanks also go to Mr. M. Steve of Battelle for his efforts in compiling this document, as well as to the NRC agreeing to publish it.

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Battelle

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SUMMARY

The fourth in a series of international Leak-Before-Break (LBB) Seminars supported in part by the U.S. Nuclear Regulatory Commission was held at the Taiwan National Central Library in Taipei, Taiwan on May 11 and 12, 1989. The Chang-Hwa Nuclear Society and the Taiwan Section of the American Nuclear Society sponsored the seminar with the encouragement of the NRC. The seminar provided information on the international policies and supporting research on LBB and updated the prior meetings held at Monterey, California in September 1983, Columbus, Ohio in October 1985, and Tokyo, Japan in May 1987. The proceedings of these seminars have been published by the NRC as NUREG/CP-0051, NUREG/CP-0077, and NUREG/CP-0092, respectively. As with the past two LBB seminars, this seminar was held in conjunction with the meeting of the International Piping Integrity Research Group (IPIRG).

This seminar was well attended, with over 100 people representing eight countries and 28 different organizations. The attendees included representatives from regulatory agencies, electric utility companies, nuclear power plant fabricators, research organizations, and academic institutions. The attendance list is in Appendix A.

Regulatory policy was the subject of presentations by Mr. G. Arlotto (U.S. NRC, U.S.A.), Dr. B. Jarman (AECB, Canada), Dr. P. Milella (ENEA/DISP, Italy), Dr. C. Faigy (EDF/Septen, France) and Dr. K. Takumi (NUPEC, Japan). A paper by Mr. K. Wichman and Mr. S. Lee of the U.S. NRC Office of Nuclear Reactor Regulation is included as background material to these proceedings; it discusses the history and status of LBB applications in U.S. nuclear power plants. In addition, several papers on the supporting research programs described regulatory policy or industry standards for flaw evaluations, e.g., the ASME Section XI code procedures. Questions following the presentations of the papers focused on the impact of various LBB policies or the impact of research findings.

Summary of Presentations on Regulatory LBB Policies or Procedures

Supporting research programs were reviewed on the first and second day by several participants from Taiwan, U.S., Japan, Canada, Italy, and France. Some of the papers in these proceedings are more regulatory or pragmatic in nature, some are more technical in nature, while others may contain some technical and regulatory aspects. Each of the papers presented in these proceedings is summarized briefly in the following paragraphs.

Summary of Regulatory and Pragmatic Papers

Mr. Guy A. Arlotto, Director of the Division of Engineering of the Office of Nuclear Regulatory Research of the U.S. NRC opened the seminar with a keynote speech entitled "LBB Application Optimization Must be Our Goal". Mr. Arlotto noted that LBB applications currently are limited to exclusion of hardware for dynamic effects from a pipe break. The NRC Advisory Committee on Reactor Safeguards recommended that to encourage a technological pursuit of evidence

that could justify potential future LBB applications, "an avenue for consideration of further extension of the LBB concept should exist". Future applications could be for containment design, ECCS design, or equipment qualification.

The paper by Mr. K. Wichman and Mr. S. Lee of the U.S. NRC's Office of Nuclear Regulatory Regulation examines the history of LBB licensing in the United States. It discusses the limitation on LBB in the U.S., LBB acceptance criteria, status of LBB regulatory development, and the status of LBB applications in the U.S. The authors note that two-thirds of the PWR's have received approval for LBB applications to the primary coolant loop. LBB has not been approved for any BWR's because of continuing concern over stress-corrosion cracking, even in TP316 nuclear steels.

The paper by Dr. D. Norris of EPRI presents an acceptance criterion for structural evaluation of erosion-corrosion thinning in carbon steel piping. This criterion is currently being considered for implementation into Section XI of the ASME Boiler and Pressure Vessel Code. This evaluation method was developed as a result of a failure of the Surry Unit 2 reactor feedwater piping, and subsequent NDE evaluations showing wall thinning in several other PWR feedwater piping systems.

The paper by Dr. K. Takumi of NUPEC in Japan summarizes a four-year program conducted for MITI on LBB applicability to carbon steel pipes. They conducted 28 straight pipe fracture experiments, and several elbow and tee cyclic loading to leakage tests. For the STS 42 carbon steel pipe investigated, it was concluded that the pipes reached limit load, hence elastic-plastic fracture analysis methods were not needed. As a final result of the program, it was concluded that the concept of LBB is applicable to carbon steel piping in LWRs.

The paper by Dr. C. Faidy, Mr. S. Bhandari, and Mr. P. Jamet discussed on-going LBB programs in France. Practical applications of LBB at the present time are quite limited in French nuclear reactors. Some fracture mechanics studies that are complete involve cracks in gas graphite reactor piping systems, primary and auxiliary line pipes in PWRs, steam generator tubes in PWRs, and pipes and main pressure vessels in liquid metal fast breeder reactors. The authors state that, although practical applications are currently limited, the French are determined to define a complete validated methodology.

The paper by Mr. M. Kozluk et al. of Ontario Hydro in Canada describes the LBB approach being used and experiences with the application to the Darlington Nuclear Generating Station. Significant material characterization and analysis was undertaken in this effort. The analysis efforts involved development of elastic solutions of the crack driving force for curvilinear through-wall axial cracks on the sides of elbows, as well as cracks in branches or tees by using finite element analysis. Finally it was noted that there was a cost benefit in the LBB analysis of approximately 15:1, reduced man rem, a more rational design, and increased licensability.

The paper by Dr. B. Jarman discusses regulatory experiences in Canada on LBB. The paper discusses the probability of failures in the large diameter (21-inch) heat transport piping. Several examples of cracked pipes and pipe components are given. It is concluded that they have a concern that "leak-before-break may become a rationale for eliminating important and effective measures in the defense in depth concept (i.e., in-service inspection) along with poorly conceived measures such as pipe whip restraints."

Mr. S.-C. Cheng of Taiwan Power Company presented a paper entitled "Overview of Chinshan Nuclear Power Station Recirculation Pipe Repair and Replacement". The paper reviewed the weld overlay repairs and pipe replacements, as well as the problems encountered in the repair and replacement procedure.

Summary of Technical Papers

The paper by Mr. Ph. Jamet et al. on "Crack Initiation and Experimental Determination of J in Bending for Elbows and Pipes of Austenitic Steel", describes a cooperative French research effort. The experiments were performed on cracked straight pipes and elbows with circumferential cracks. The obtained results showed that for shorter circumferential through-wall cracked pipe and elbows in bending, the failure loads were below those predicted by the net-section-collapse equation, unless the flow stress of the material is lowered.

The paper by Dr. G. Wilkowski, Mr. K.-S. Chao, Dr. F. Brust, and Mr. P. Gilles (representing Battelle, Taiwan Power Company, Battelle, and Framatome, respectively) compared predicted moments at crack initiation and maximum load for through-wall circumferentially cracked pipe in four-point bending. Five different J-integral estimation scheme analyses were used to compare to 16 pipe experiments involving cracks in carbon steel base metal, stainless steel base metal, carbon steel SAWs, and stainless steel SAWs. The pipe diameters varied from 100 to 914 mm. The results showed that the GE/EPRI method was the most conservative, and the Paris and LBB.NRC methods were the least conservative. The LBB.ENG and LBB.NRC methods (developed in the NRC's Degraded Piping Program) were slightly conservative but were still reasonably accurate.

Dr. P. Milella of ENEA summarized the LBB research efforts in Italy from 1981 to 1987. Some of the results are: (1) the net-section-collapse method seems to underestimate the maximum failure loads of carbon steel pipes while it accurately predicts that of stainless steel pipes, (2) the GE/EPRI method is a valid method to predict the crack opening displacement and maximum moment for pipes, (3) the Tada-Paris method seems to overestimate the actual leak area in pipes particularly when at bending moments where plasticity occurs, (4) A106 carbon steel pipe can experience severe toughness loss, probably from dynamic strain aging at 280 C, and (5) leak areas remain well below 10 percent of the pipe cross section with through-wall cracks smaller than 160 degrees and loads within the ASME Section III normal operating stress loads. An on-going experimental program to verify pipe fracture under inertial loading was also summarized. This program will continue from 1988 to 1992.

Dr. J. Pan of the University of Michigan presented a paper on the "Deformation Mode and Crack Opening Displacement for Circumferentially Cracked Pipes in Bending and Tension". This presentation showed the results of an NRC-funded program to evaluate the effects of circumferential crack opening and pipe ovalization at fixed ends, i.e., a circumferential crack at a nozzle weld. Elastic shell analyses were conducted. The elastic analysis showed that the pipe diameter, in the bending plane, always increased even with short cracks. The observations of decreasing pipe diameter in the bending plane during pipe fracture experiments is believed to be due to a prebuckling elastic-plastic behavior of the uncracked pipe. This was only observed in pipes with short circumferential cracks where the bending loads approached the buckling loads of the pipe.

The paper by Dr. R. Wu and Dr. Y. Chen of the Materials Research Laboratory in Taiwan discussed the "Stress Intensity Factors of Axisymmetric Circumferential Cracks in Cylinders". In this paper an elastic stress intensity factor solution by shell analysis was compared to finite element analyses for circumferentially surface-cracked pipe in axial tension with different stress distributions through the pipe thickness. The solutions generated were for inside radius (R_i) to pipe thickness (t) ratios of 5 to 20. The magnitude of the error was under 5 percent for flaw depths less than 60 percent of the pipe thickness, and R_i/t of 5 to 20.

Dr. K. Ting and Dr. J.-S. Yu of the Institute of Nuclear Energy Research of Taiwan presented results on ductile fracture analysis by strain energy density functions. They applied the strain energy density approach to a C(T) specimen used in a past Battelle/NRC finite element round robin. They found that the strain energy density function can be used to predict the stable crack growth in ductile fracture analysis, that the approach is easily extended to three-dimensional geometries, and that only the stress-strain curve is needed in the input.

Dr. C. G. Tseng of the National Taiwan Institute of Technology reviewed results of ductile fracture experiments on through-wall cracked pipe. The experiments were conducted on 1.5-inch diameter Schedule 20 TP304 stainless steel pipe. The pipe had a circumferential through-wall fatigue crack in the base metal that was approximately 32 percent of the pipe circumference. The pipe was loaded in four-point bending at room temperature. The J-R curve calculated from this experiment was 60-percent higher than that from the 4-inch pipe experiments conducted at Battelle for EPRI. The initiation load was found to be 97 percent of the maximum load, which is consistent with past small diameter stainless steel pipe tests.

SESSION 1

CO-CHAIRMEN

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LBB APPLICATION
OPTIMIZATION MUST BE OUR GOAL

by

Guy A. Arlotto

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U.S. Nuclear Regulatory Commission

Good morning. I would like to thank our hosts for arranging this seminar on leak-before-break, offering us once again the opportunity to discuss this important topic within the environment of an international forum.

Before getting on with the prepared text of my speech, I would like to make a few personal comments. It has been a personal pleasure for me to be here in Taiwan. I renewed old friendships and made many new friends. Regarding the hospitality of our Taiwan hosts during this conference, I cannot say enough. IPIRG is truly an international program having representatives from nine countries. Therefore, here in Taiwan, the representatives from eight countries are guests. I know that I speak for all guests when I say that our Taiwan hosts have been most kind and generous. We, the guests, are most grateful.

As you may know, this is the fourth international seminar since 1983 emphasizing leak-before-break technology. There has been significant progress since the 1983 meeting in Monterey, California -- progress both in developing the technology and in putting that technology to work in the regulatory process.

This morning I would like to explore with you some of the technical advances that have led the U.S. NRC to accept leak-before-break for piping that can pass rigorous acceptance criteria. I also would like to touch on some of the areas that we feel need further work. My discussions will not however, deal with the application of leak-before-break to steam generator tubes, containments under severe accident conditions or any component other than piping. I hope to challenge those of you in the international research community to bring about the technical advances needed to develop more fully this technology to the point of optimization for application and to bring about an international consensus on the technical basis for leak-before-break.

It is useful to investigate what prompted the U.S. NRC to even consider leak-before-break. Historically, going back to the Shippingport reactor in the 1950s, we have assumed the double-ended guillotine pipe break as the design basis for certain engineered safety features as part of our defense-in-depth approach to reactor safety. Applying this assumption consistently in the 1960s affected not only the design of the engineered safety features but certain other plant hardware. In the late 1970's we began to recognize the problems associated with the postulated double-ended guillotine break of the large diameter primary coolant pipes -- problems that had little to do with engineered safety feature design. For example, a postulated break in a cold

leg pipe of a pressurized water reactor would produce significant lateral forces on the reactor pressure vessel and its supports. It also would result in reversed flow through the core, raising questions about the continued performance of the vessel internals. This general problem was called the asymmetric blowdown problem, and the NRC identified it as Unresolved Safety Issue A-2.

The issue revolved around the postulated double-ended break of the large pipes. If it could be shown that the likelihood of this break was sufficiently low, the issue could be resolved. Thus, both the NRC and the Westinghouse Owner's Group undertook research and analysis efforts to examine the technical basis for reconsidering the validity of requiring this break to be postulated. We were, in fact, considering whether leak-before-break was a valid concept.

There were many questions raised that required resolution to demonstrate the validity of the leak-before-break concept. Could pre-existing manufacturing defects actually extend due to the service loadings and environment? What size defects might exist in the pipe as it went into service? How could we predict the critical defect size for given loadings? What role could inservice inspection play in assuring leak-before-break? How reliable were present day inservice inspection techniques? Were their results reproducible? Could we rely on leak detection systems to signal leakage early enough to permit safe shutdown of the system before a crack could grow to a critical size? What load combinations should be considered in the analysis? What margin of safety should be included in the analyses? These questions, among others, had to be addressed before we could move to accept even a limited application of leak-before-break.

After several years of intensive research and much discussion of the results and the overall philosophy, the U.S. NRC staff became convinced that the leak-before-break concept was valid at least for the large diameter primary coolant loop piping of PWRs. Accordingly, the NRC undertook a multi-step approach to accepting leak-before-break. First, the Asymmetric Blowdown Load Unresolved Safety Issue A-2 was successfully concluded and those plants affected by that issue were granted exemptions from postulating these large pipe breaks. Second, we initiated rulemaking to change the applicable general design criterion of our regulations (GDC-4), eliminating the requirement to provide protection against the dynamic effects of postulated double-ended breaks in the large diameter primary coolant loop pipes of PWRs. This was the so-called limited scope rule which was used to remove pipe whip restraints and jet impingement barriers placed to mitigate these pipe rupture effects. Finally, we initiated rulemaking to make an additional revision to that general design criterion, eliminating the need to consider dynamic effects of postulated breaks in all piping systems that could pass rigorous acceptance criteria.

It is important to note that our acceptance of the leak-before-break concept has been limited to those dynamic effects associated with the postulated double-ended pipe ruptures. Our current position specifically excludes extending the application to containment design requirements, ECCS sizing requirements, or environmental qualification requirements for electrical and mechanical components. To the best of my knowledge, this is consistent with current international practices.

As some of you may know, in an effort to get the public in the United States involved in a reexamination of this position, we requested public comment on potential safety benefits that might be derived from and the technical basis that would justify extending leak-before-break to areas other than dynamic effects. The NRC staff concluded that there was not adequate technological evidence to justify further rule changes. The NRC Advisory Committee on Reactor Safeguards (ACRS) recently reviewed the staff's recommendations, it agreed with the staff's conclusions and concurred in publication of a policy statement. However, to encourage continued technological pursuit of evidence that could justify potential future changes, the ACRS recommended that, and I quote from the ACRS letter to the Commission, "an avenue for consideration of further extension of the LBB concept should exist." end quote.

The Commission recently approved a policy statement on additional applications of leak-before-break which adopts the staff's conclusions as modified by the ACRS recommendations. From a practical viewpoint this means that we will not permit leak-before-break approach for containment design, ECCS design or equipment qualification at this time, and I emphasize "at this time."

As I mentioned earlier, even for dynamic effects, the NRC's regulations permit applying leak-before-break only to piping systems that meet rigorous acceptance criteria. These criteria effectively limit applying leak-before-break to piping systems in which we do not expect cracking due to service conditions, for which we understand the service conditions, and for which we understand and can bound the effects of the loading conditions on the piping. There is an additional criterion that comes into play if a system satisfies the above. This criterion requires a fracture mechanics analysis

to be performed to demonstrate that if a through-wall crack were to develop, it would not result in a double-ended guillotine break even if subjected to low probability, severe dynamic loads such as the safe shutdown earthquake. The results of this analysis further justify applying leak-before-break.

The acceptance criteria become very important to the overall approach. The fracture mechanics analysis provides additional protection even though we do not expect any cracking in lines that meet the acceptance criteria. This is consistent with the NRC's defense in depth philosophy but more importantly it sets the tone that I believe is the proper one - given the reactor safety tradition of our regulations that has resulted in a super safety record, we must exercise caution in making changes.

As we take the bold step of justifying the extension of leak-before-break to piping other than the primary coolant loop, because of the massive length involved and the variability of service conditions, we must be even more careful to assure consideration of all sources of service loadings and operational conditions that go beyond those anticipated at the design stage. For example, recently a leak developed in a safety injection line. After the fact evaluations indicate that the crack was due to thermal fatigue caused by an intermittently malfunctioning check valve. This could be characterized as a maintenance problem, and indeed adequate inspection and maintenance of these valves may have avoided or limited the problem. However, the fact remains that a "once unanticipated" service condition led to thermal fatigue cracking in a line that, on first examination, would have been considered to meet the leak-before-break acceptance criteria. This example illustrates that we must be extremely careful in applying not only

the literal interpretation of the acceptance criteria but also their spirit. It also illustrates that the overall approach can provide an adequate margin against failure if applied properly because, after all -- the leak was detected and the crack would have remained stable even if a demanding load such as the safe shutdown earthquake had occurred.

There have been other examples of unanticipated service conditions that make us ever mindful that we must be careful in evaluating a piping system against the acceptance criteria. The unanticipated motion of a surge line due to thermal stratification is a case in point. In this case, actual pipe deformation took place that was not expected or evaluated prior to the fact. These above examples are real - they have occurred in U. S. plants. I believe they can occur in other countries.

While some service experiences show us that we must be cautious in applying leak-before-break, our research results continue to clarify how cracks develop and grow, and how to evaluate the fracture behavior of cracked pipe. For example, it has been suggested that 316 NG stainless steel is a material far superior to the traditional 304 stainless steel for service in BWR's. Indeed, our research confirms that 316 NG shows significantly improved resistance to crack initiation. However, further research indicates that if a crack initiates and begins to propagate, it becomes much more difficult to arrest that growth for the 316 NG material than for 304. While simply "cleaning up" the environment (that is the water chemistry) will stop IGSCC propagation in the 304 material, our research indicates that it takes a much cleaner environment to arrest crack propagation given a crack in the 316 NG. This research result clearly shows that simply replacing 304 stainless steel

pipng with 316 NG is not the panacea for stress corrosion cracking that we had hoped and that some people still believe.

Conservative probabilistic modelling of IGSCC in BWR recirculation loop piping fabricated from 316 NG indicates that pipe ruptures are not very likely during about the first 30 years of service, but thereafter the probability increases dramatically. One estimate, which did not account for inservice inspection, estimated the pipe rupture probability after 40 years in a recirculation line made from 316 NG as about 10^{-3} (one chance in a thousand). It should be noted that this estimate has not been subjected to an indepth peer review. However, if substantiated, it can have a significant effect on license extension that is being actively pursued in the United States.

We also are learning more about how material properties degrade with time. For example, our research shows that fracture toughness loss in cast stainless steels is due to different mechanisms at higher temperatures than at typical LWR operating temperatures. Thus, to specify artificially aging these materials at temperatures significantly above 400°C does not appear fruitful because results derived would be questionable if not absolutely wrong. Also, through our research we have discovered that some cast stainless steels have a significant Niobium content and that these materials appear to be more susceptible to thermal aging effects than materials that do not contain Niobium. These two research results, namely the limitation on artifically aging at higher temperatures and the effects of Niobium,

increase our knowledge, giving us greater confidence in evaluations of piping systems and components made using cast stainless steels and, ultimately, justifying better decisions regarding leak-before-break applications.

Turning to pipe fracture analysis methods, we have recently completed the second phase of the Degraded Piping Program. This 4-year effort significantly improved the pipe fracture experiment data base and led to major improvements in the fracture analysis methodology. Yet, despite these achievements, there remain some troubling issues. For example, as many of you know, finite element analysis predictions of the load-deflection behavior of through-wall cracked pipe under 4-point loading consistently under predict the experimental results. If what we believe are currently our most sophisticated analysis methods do not predict well the results for relatively simple pipe fracture experiments, then we must question the technical basis for analyses of more complicated configurations.

The responsible conclusion that flows from the above - more must be done to resolve the problems in these analyses so that we can confidently apply them to complex configurations for which we do not have experimental benchmarks. To simply say that we have a conservative analysis based on limited experimental data is not enough. We must understand the technology in the real world to justify best estimate analyses.

I have concentrated here on examples pertinent to the NRC's leak-before-break regulations. However, these same issues pertain to the approaches taken in other countries when considering leak-before-break. The safety injection line cracking incident shows us that we must be careful to

consider all mechanisms or sources of loading when evaluating the integrity of piping -- whether you are using the NRC's criteria or are performing a detailed fatigue analysis to justify continued operation. The aging of cast stainless steel examples show that we must understand the processes that control material degradation as well as pipe fracture behavior before we can confidently apply this technology to every class of material or service condition.

Since these problems appear to be basic and to the best of our knowledge not country specific, it appears logical to resolve these problems as an international community because each country can be impacted. I attended the International Piping Integrity Research Group meetings earlier this week and was pleased to see that indeed this group is functioning more as a unit rather than as a collection of individuals or nations. I see this as evidence that we can work together to solve these problems. Our collective efforts seem to have the desired synergism, producing more progress than could be expected from each effort taken separately. This is the only way we can expect to arrive at an internationally accepted technical basis for evaluating piping integrity and for optimizing the acceptance and application of the leak-before-break concept.

Optimizing really must be our goal and it is the most difficult challenge. This is evident if you look at both ends of the spectrum. On the one end, you can ignore leak-before-break and you miss the opportunity to realize economic and safety gains; on the other end, you can permit its application to everything for all purposes and you may have a result that is counter to safety. The challenge is how to optimize to achieve maximum justified benefits.

I indicated at the beginning of this speech that I hoped to challenge those of you in the international research community to bring about the technical advances needed to develop more fully this technology. I have given you some examples of areas where we as regulators, as well as responsible utilities and their agents, must be careful in applying leak-before-break. We are relying on the research community to develop better ways to identify potential loadings and service conditions before they result in cracking. I also have suggested several areas where the basic technology needs further development -- aging of cast stainless steel and pipe fracture analyses being cases in point. However, there are other areas that also warrant further attention.

In the field of Ultrasonic Testing, the past research, including the comprehensive international program for inspection of steel components, PISC I and II and the on-going PISC III programs, has improved this technology significantly, but there remains much to do. We still have significant problems in reliably inspecting cast stainless steel for example. Reproducibility of test results including consistent interpretation of signals is a worthy goal.

Environmentally Assisted Cracking remains a problem area. We now have evidence that stress corrosion cracks initiated in Inconel welds may propagate into ferritic steel nozzles. Further, there remain questions about the effects of LWR environments on fatigue crack initiation and about the effects of variable amplitude loading on estimating fatigue crack growth.

These areas, warrant further study; there may be others. There are several groups both country specific and international that are studying some of these problems. However, we must not relent -- we must continue to push back the frontiers of these technologies. We must develop a clear understanding of how piping behaves when subjected to the potential range of service conditions and we must improve our ability to predict that behavior.

In conclusion let me leave you with the following.

In 1985 at the Leak-Before-Break Meeting in the United States at Columbus, Ohio, I closed my speech by noting that the rewards of leak-before-break could be great, resulting in a win-win situation -- greater safety at reduced cost. Today, I still believe those words. We have moved forward since that 1985 meeting but there is more to be done. Therefore, I will repeat myself -- this is a most important subject that needs the best we can give, openly and objectively. The rewards could be great, and we should strive toward the goal of greater safety at reduced cost. Optimizing the application of leak-before-break would accomplish this, therefore optimization must be our goal. It is difficult. The application may be different in each country. However, the challenge is to develop the scientific knowledge and the associated expert insights for optimizing application of the LBB technology that is justified and that supports responsible regulatory decisionmaking. The challenge falls on the shoulders of the technological experts of the international community. In my judgment, it is yours.

Thank you.

RESULTS OF THE JAPANESE CARBON STEEL PIPE FRACTURE PROGRAM

by

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Toranomom 4-3-13, Minato-ku, Tokyo, Japan

Abstract

Carbon steel pipe fracture program was conducted from April 1985 to March 1989 to establish the verification of LBB for LWR primary piping system in Japan. The Verification Test Committee of the program was organized by NUPEC (Nuclear Power Engineering Test Center) under the sponsorship of MITI (Ministry of the International Trade and Industry), as shown in Fig. 1. The committee consists of universities, institutes, utilities and fabricators. Main tasks of the program schedule are shown in Table 1.

Pipe Rupture Test was performed on STS 42 carbon steel pipes with circumferential cracks. Fracture behavior test machine with compliance device is illustrated in Fig. 2. The test conditions are summarized in Table 2. The total quantity of the pipe specimens was 28. The results of net-section stresses at maximum loads are shown in Fig. 3. The net-section stresses are independent of crack angles. Therefore, the net-section stress failure criterion is applicable to STS 42 carbon steel pipe.

LBB Verification Test was performed on 6-inch diameter Straight, Elbow and Tee pipes subjected to cyclic load. High temperature and high pressure water was flowed inside the pipes. The test matrix and water supply system are illustrated in Figs. 4 and 5. Table 3 shows the test condition and results of LBB Verification Test. The crack growth behavior of all pipes was stable at leakage, even though large size cracks are existed in pipes. Appearance of outer surface of Tee pipe is shown in Fig. 6. The crack penetrated the wall-thickness at 502 cycles, and then 12 cycles were added after leakage at ambient temperature.

The scenario of LBB for carbon steel pipe is basically equivalent to that for stainless steel pipe, as shown in Fig. 7. From the crack growth analysis and fracture evaluation, the design criteria for carbon steel pipe are categorized into three parts; No-Requirement, Guillotine Break and Subcritical Through-wall Crack. The crack growth amount after 80 years for BWR main steam line is shown in Table 4. The period is twice the design life of plant operation. All cracks did not penetrate the wall-thickness of the pipes after 80 years. Leakage does not occur for all pipes.

The standard of LBB of carbon steel pipes for BWR main steam line is shown in Table 5. The "N" means no leak and non-break, and "A" rupture. Almost all pipes are "N", except for 2-inch diameter pipe with high stress. We obtained such results not only the main steam line but also other lines and PWR carbon steel lines.

It is concluded that the concept of LBB is applicable to carbon steel piping in LWR.

Results of the Japanese Carbon
Steel Pipe Fracture Program

by K. Takumi
NUREC

May 11, 1989

Taipei

This presentaiton consists of ;

★ *Introduction of the Program*

★ *Pipe Rupture Test*

★ *LBB Verification Test*

★ *LBB Standard*

and

★ *Conclusions.*

Organization of the Project

MITI



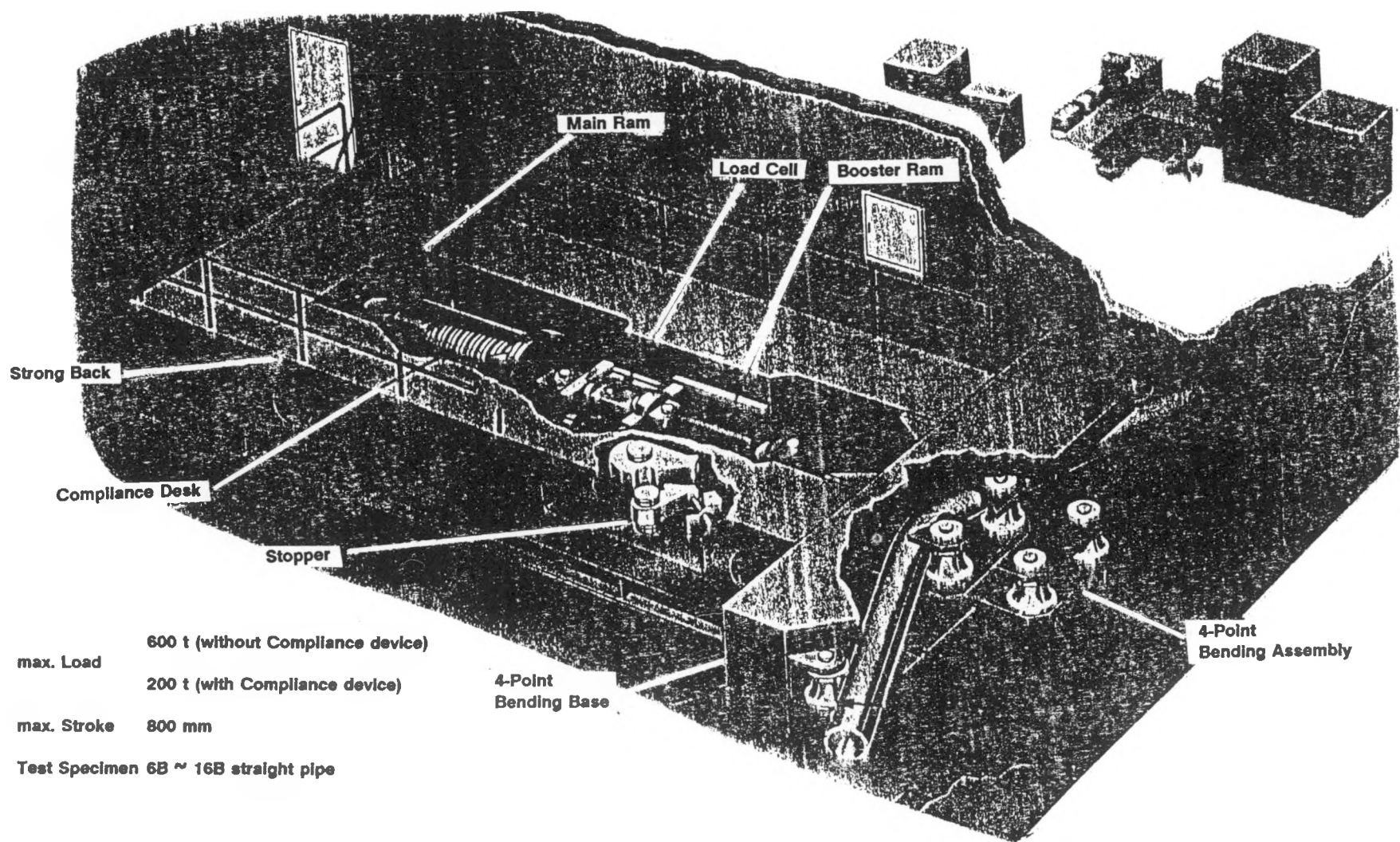
NUPEC	
Verification Test Committee	
University	Univ. of Tokyo Tokyo Inst. of Techn.
Institute	JAERI, CRIEPI
Utility	Tohoku, Tokyo, Chubu, Kansai, Chugoku, Shikoku, Kyushu, JAPC
Fabricator	Toshiba, Hitachi, MHI, BHK, IHI, MAPI

Table Main tasks of program schedule

Fisical Year	1985	1986	1987	1988
Planing and Basic Data Test	[Bar spanning 1985 to mid-1986]			
Numerical Analysis	[Bar spanning from mid-1985 to mid-1988]			
Pipe Rupture Test (RT, 300°C)		[Hatched bar spanning from mid-1986 to mid-1987]		
LBB Verification Test				[Hatched bar spanning from mid-1987 to mid-1988]
Evaluation and LBB Standard			[Hatched bar spanning from mid-1987 to mid-1988]	

Pipe Rupture Test

(Oct. 1986~ March 1988)



max. Load 600 t (without Compliance device)

200 t (with Compliance device)

max. Stroke 800 mm

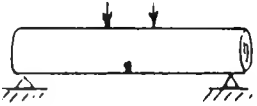
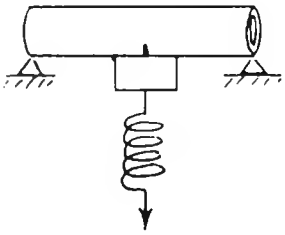
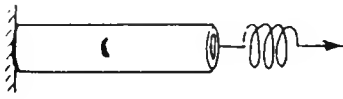
Test Specimen 6B ~ 16B straight pipe

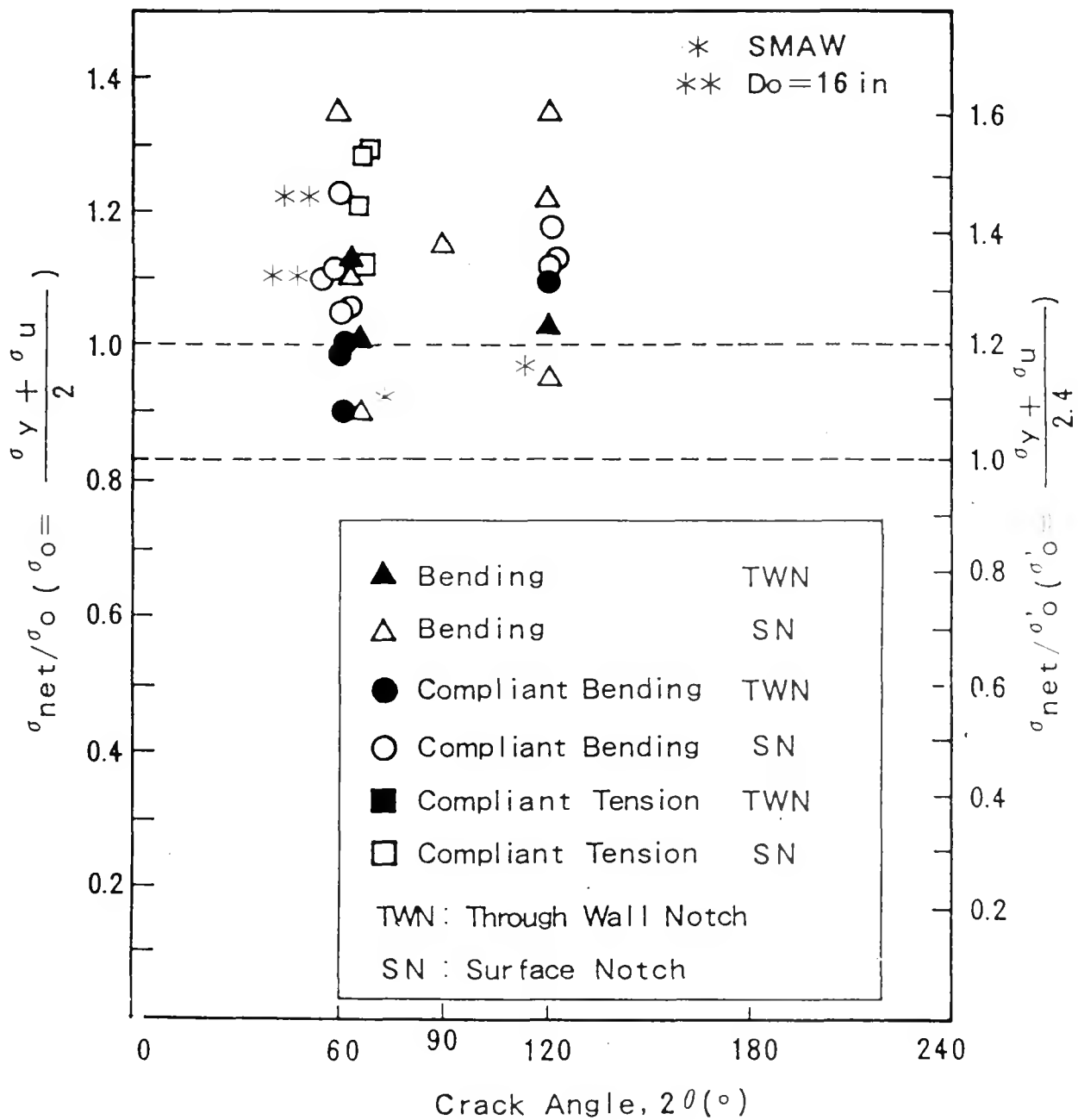
4-Point Bending Base

4-Point Bending Assembly

FRACTURE BEHAVIOR TEST MACHINE

Table Pipe fracture test (Straight pipes)

	Material	Diam. inch	Temp. °C	Compliance tonf/mm	Quantity
Bending 	STS 42	6	RT	—	6
		16	RT	—	2
	SMAW	6	RT	—	2
Bending with Compliance 	STS 42	6	RT	1	6
			300	5	2
			RT	5	2
	16	RT	1	1	
		300	1	1	
Tension 	STS 42	6	RT	1	2
			300	1	2
Tatal					28

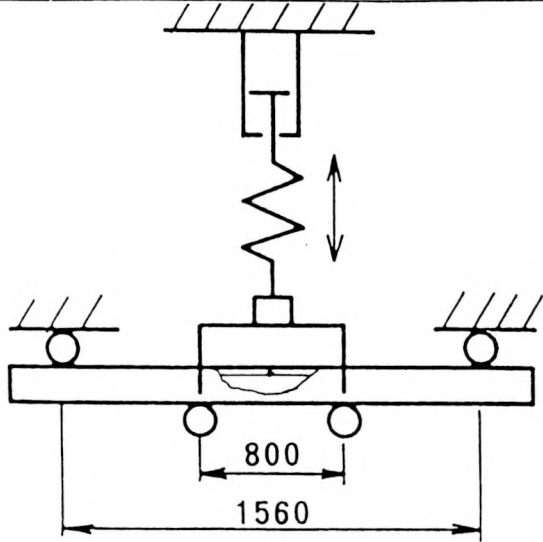
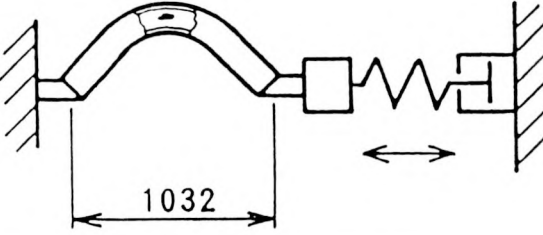
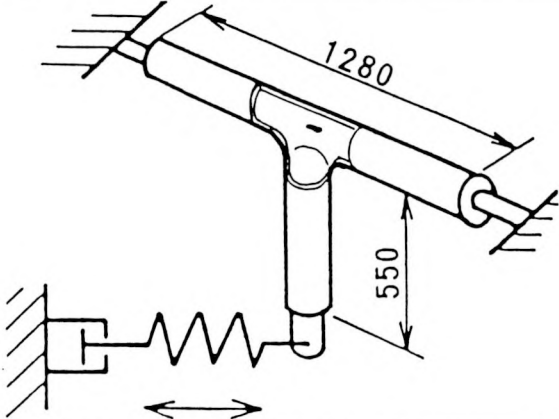


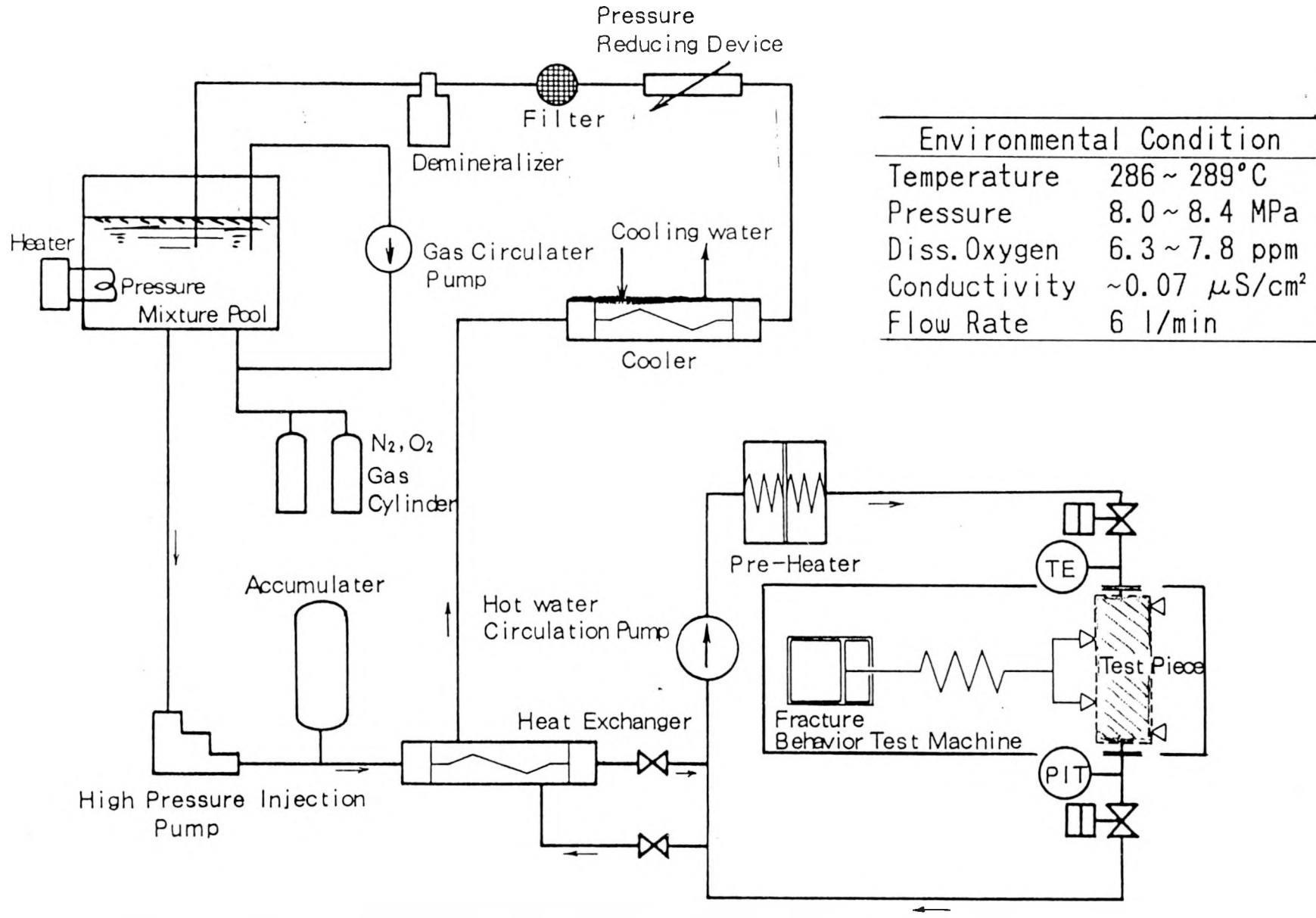
NET SECTION STRESSES AT MAX. LOAD

LBB Verification Test

(April 1988 ~ March 1989)

Table Illustration of LBB Verification Test

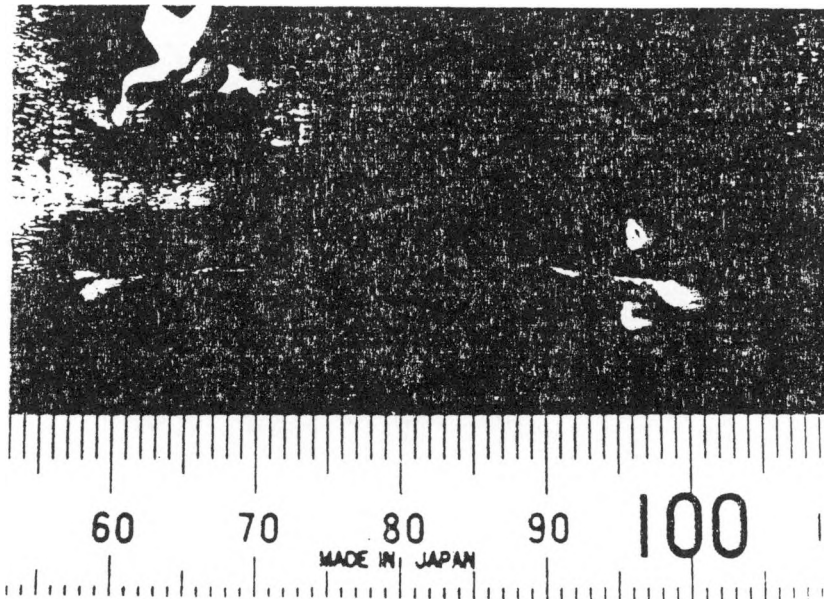
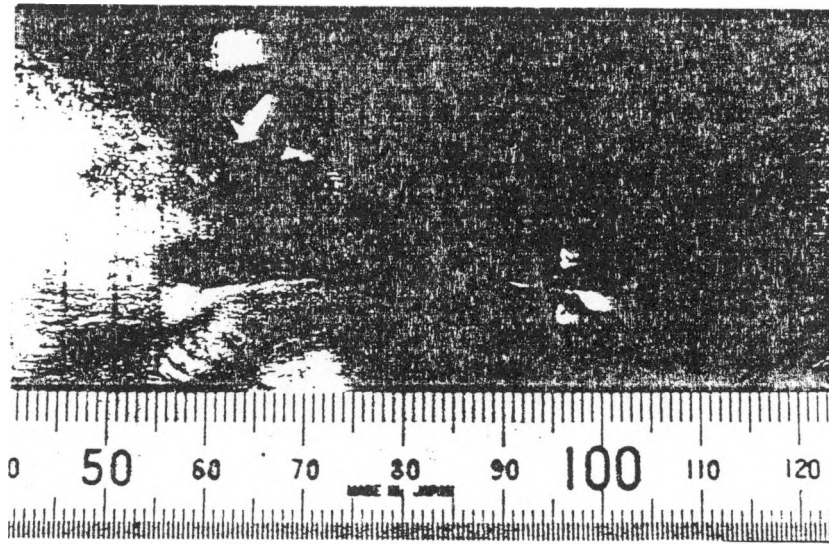
	Cyclic stress : 0.5 ~ 1.64 S_m With high temperature water	Quantity of Specimen
<p>Straight Pipe</p> <p>STS42</p>		<p>3</p>
<p>Elbow</p> <p>SFVC2B</p>		<p>1</p>
<p>Tee</p> <p>SFVC2B</p>		<p>1</p>



Water Supply System and Environmental Condition

Table Result of LBB Verification Test

Specimen	Surface Crack		Load Range		Period min./cyc	ΔK kg-mm ^{3/2}	Cycle at Leakage	Cycle after Leak	Crack Growth
	length (mm)	depth a/t	cycle	tonf					
Straight Pipe 6B	82° (102)	0.8	1~3	3.6~8.9	4~10	57	425	+15	<u>stable</u>
			4~5	3.6~13.9		117			
			6~425	3.6~18.2		168			
		0.8	1~2302	3.9~18.9	3.5~4.0	111	2302	+14	<u>stable</u>
		0.5	1~1425	3.4~17.4	3.4~4.0	189	1425	+16	<u>stable</u>
Elbow 6B	102	0.8	1~402	2.2~6.7	3.0~8.0	—	15870	+13	<u>stable</u>
			403~9012	2.2~6.7	1.5~3.3	—			
			9013~15870	2.2~7.2	1.3~1.5	—			
Tee 6B	102	0.8	1	1.0~4.9	3.3	82	502	+12	<u>stable</u>
			2	1.0~5.9	3.6	101			
			3	1.0~6.4	3.6	111			
			4~502	1.0~6.9	2.6~4.0	121			



APPEARANCE OF OUTER SURFACE AFTER STEP 3 TEST (TEE)

LBB Standard

(April 1987 ~ March 1989)

SCENARIO OF LBB CRITERIA

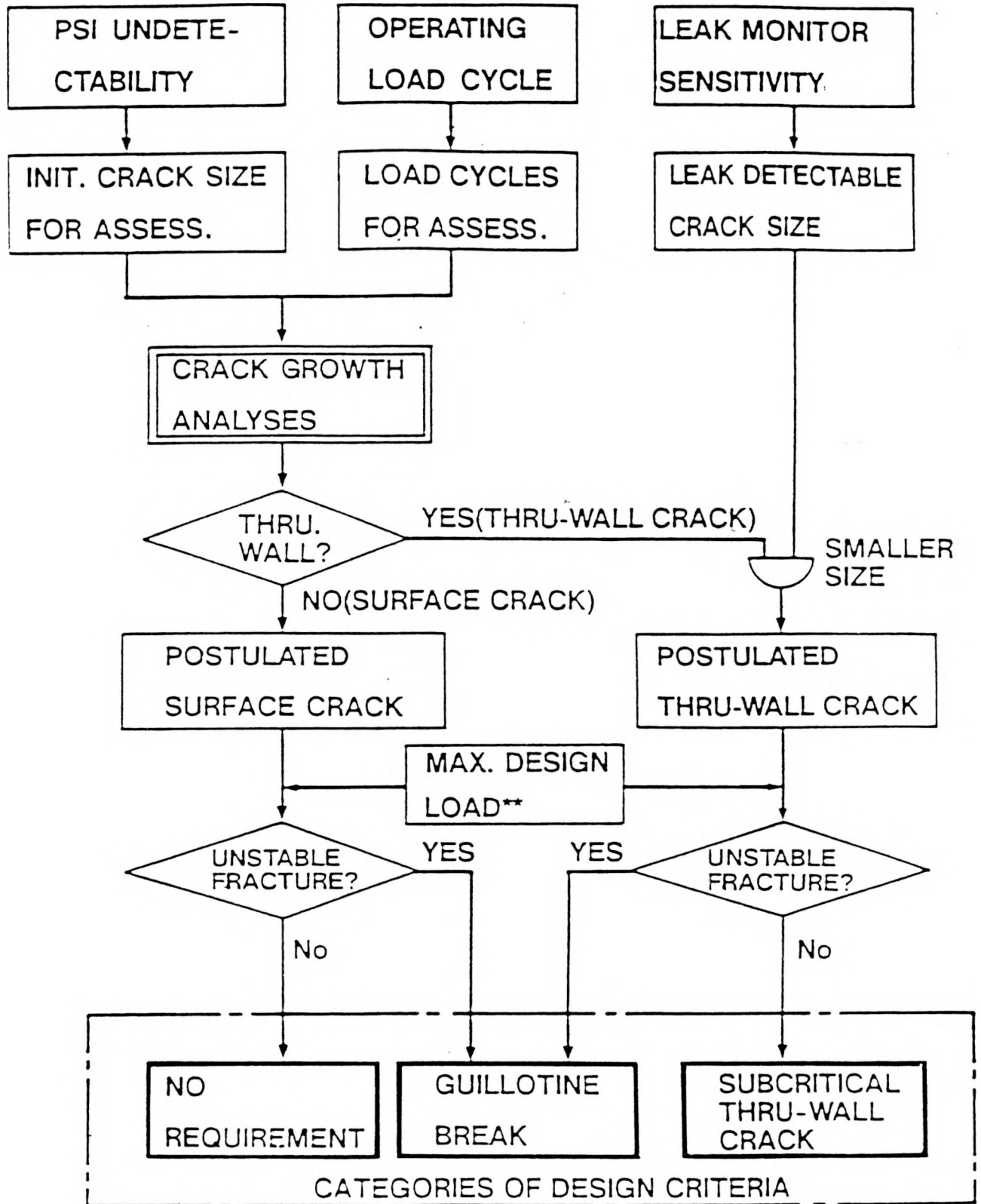


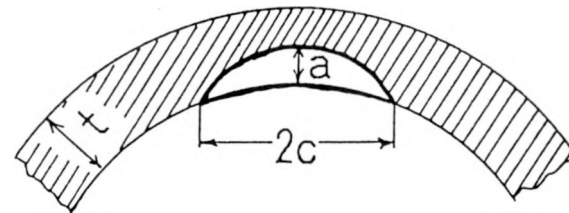
Table Crack growth result for BWR Main Steam Line

Pipe Size	Mater.	Diam. (mm)	Thickn. (mm)	Initial Crack		After 80 years		limit σ/S_m
				a (mm)	c	a (mm)	c	
26B S/80	STS 49	660.4	33.3	3.33	8.33	6.38	10.62	3.34
24B S/80		609.6	31.0	3.10	7.75	6.04	9.93	3.34
20B S/100		508.0	32.5	3.25	8.13	6.26	10.38	3.33
10B S/100	STS 42	267.4	18.2	2.00	5.00	4.33	6.45	3.38
6B S/120		165.2	14.3	2.00	5.00	4.36	6.52	3.36
4B S/160		114.3	13.5	2.00	5.00	4.36	6.54	3.32
2B S/160		60.5	8.7	2.00	5.00	4.51	6.82	2.86

29

$$a=0.1t$$

$$2c=0.5t$$



initial crack

Table Result of LBB concept of carbon steel pipe
for BWR Main Steam Line

Diam. inch		2	4	10	12	14	16	18	20	24	26
Diam. mm		60.5	114.3	267.4	318.5	355.6	406.4	457.2	508.0	609.6	660.4
t (mm)		8.7	13.5	18.2	21.4	23.8	26.2	29.4	32.5	31.0	33.3
thru-wall crack 2θ		—	—	—	—	—	—	—	—	—	—
Stress limit ($\times S_m$)		2.86	3.30	3.38	3.39	3.39	3.39	3.39	3.33	3.34	3.34
Applied Stress	$\sigma_t = 0.5S_m$ $\sigma_b = 0$	N	N	N	N	N	N	N	N	N	N
	$\sigma_t = 0.5S_m$ $\sigma_b = 0.5S_m$	N	N	N	N	N	N	N	N	N	N
	$\sigma_t = 0.5S_m$ $\sigma_b = 1.0S_m$	N	N	N	N	N	N	N	N	N	N
	$\sigma_t = 0.5S_m$ $\sigma_b = 1.5S_m$	N	N	N	N	N	N	N	N	N	N
	$\sigma_t = 0.5S_m$ $\sigma_b = 2.0S_m$	N	N	N	N	N	N	N	N	N	N
	$\sigma_t = 0.5S_m$ $\sigma_b = 2.5S_m$	A	N	N	N	N	N	N	N	N	N

A ; Rupture

N ; No leak and no rupture

(surface crack does not penetrate the pipe wall)

Conclusions

1. From Pipe Rupture Test

Fracture strength for carbon steel pipe is predicted by the net-section stress criterion.

2. From LBB Verification Test

Growths of initially large cracks in straight, elbow and tee pipes were stable. LBB was verified.

3. LBB Standard

The concept of LBB is applicable to carbon steel piping in LWR

Background and Status of Leak-Before-Break Applications
in U.S. Nuclear Power Plants

by

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and
Sam Lee

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U. S. A.

Historical Background

The current design basis for U.S. nuclear power plants evolved as various safety concerns were identified. Containment structures in the early days of commercial nuclear power were designed solely to confine the fluid release corresponding to the instantaneous double-ended guillotine break (DEGB) of the largest pipe in the reactor coolant system (RCS). The DEGB was a convenient concept that provided a reasonable upper-bound of the global effects of postulated accidents. Later on, when the concerns of providing an adequate emergency core cooling system (ECCS) emerged, the global effects based on the DEGB concept were utilized for the design of the ECCS. With the increased use and acceptance of the DEGB concept, the postulated DEGB for real nuclear piping was given the appearance of a credible event. The next logical extension then was to raise concerns about protection from the resulting local dynamic effects due to the postulated DEGB. Local dynamic effects included pipe whipping, jet impingement, local subcompartment and vessel cavity pressurizations, pipe break reaction forces, and decompression waves in the fluid.

To address the concerns of local dynamic effects of the DEGB, nuclear piping was postulated to have pipe breaks at numerous locations along its length. Protection against subsequent dynamic effects was then provided by constructing hardware such as pipe whip restraints and jet impingement shields. (Figure 1 shows three typical pipe whip restraint designs.) Snubbers were also installed to provide restraint of the DEGB pipe break reaction forces.

History of "Leak-Before-Break" Licensing

In 1975, the NRC staff designated Unresolved Safety Issue (USI) A-2, "Asymmetric Blowdown Loads on Reactor Primary Coolant Systems", to highlight the concerns of decompression waves resulting from a DEGB postulated to occur at certain critical locations. The nuclear utilities formed owners groups to address USI A-2. Using advanced fracture mechanics techniques to evaluate the tough nuclear piping, the Westinghouse Owners Group determined that a DEGB of the primary loop piping of a pressurized water reactor (PWR) would not occur mechanistically. In Generic Letter 84-04, dated February 1, 1984, the NRC

staff agreed that the DEGB of the PWR primary loop piping was unlikely to occur, provided it could be demonstrated by deterministic fracture mechanics analyses that postulated small through-wall flaws in plant-specific piping would be detected by the plant's leakage monitoring systems long before the flaws could grow to unstable sizes. The concept underlying such analyses is referred to as "leak-before-break" (LBB).

The NRC staff also recognized that the installation of protective devices might not be the most cost effective method in achieving the desired level of safety. This is because (1) the pipe whip restraints and jet impingement shields tend to restrict access of the piping for inservice inspection, (2) if these protective devices are removed for inspection and reinstalled, there is a potential risk of damaging the piping and other safety-related components in the process, (3) if these protective devices are reinstalled incorrectly, they may impede piping thermal movement and result in overstress, (4) snubbers may lockup and impede piping thermal movement, (5) protective devices result in more difficult access for inservice inspection and incur higher occupational radiation, and (6) public risk reduction in the absence of protective devices is estimated to be very low. Thus, in the short term, the NRC permitted utilities using LBB technology to request exemptions from installing protective devices on the PWR primary coolant loop. In the long term, the NRC staff initiated a rule change to General Design Criterion 4 (GDC-4) of Appendix A of 10 CFR Part 50 of the U.S. regulations to permit the application of LBB in nuclear piping.

On April 11, 1986, a final "limited scope" rule was published amending GDC-4 to permit the use of analyses to eliminate from the design basis the dynamic effects of postulated pipe ruptures of primary coolant loop piping in PWRs. On October 27, 1987, a final "broad scope" rule was published amending GDC-4 to permit the use of LBB analyses in high energy piping (i.e., pressure exceeding 275 psi or temperature exceeding 200°F) in nuclear power units.

While the application of LBB permits the elimination of associated local dynamic effects of the DEGB from the design basis, the global effects of the DEGB are retained. Thus, after LBB is demonstrated, (1) the containment must continue to be designed to withstand all global effects (such as pressure, temperature, and flooding) up to and including the rupture of the largest pipe in the RCS; (2) the heat removal and mass replacement capacity of the ECCS must continue to be designed to accommodate pipe ruptures up to and including the rupture of the largest pipe in the RCS; and (3) the environmental qualification (EQ) of equipment must continue to be based on all global effects (such as pressure, temperature, flooding, humidity, chemical environment, and radiation) resulting from pipe ruptures. Although, the broad scope rule permits the utility to request an exemption from the EQ requirements based on LBB, the NRC staff is not allocating resources to such EQ exemption reviews until the safety benefits associated with such an action can be demonstrated.

Although the concept of LBB may seem intuitive, LBB licensing evaluations require adequate engineering margins to account for uncertainties inherent in the analyses and leakage detection capability. The limitations and acceptance criteria for LBB licensing used by the NRC staff are discussed in NUREG-1061, Volume 3, and summarized in the following sections.

"Leak-Before-Break" Limitations

The application of LBB is limited to piping that is not susceptible to various degradation mechanisms. From the NRC's experience, a significant portion of any LBB review involves the evaluation of the susceptibility of the candidate piping to various degradation mechanisms.

The LBB approach is not considered applicable to piping that is particularly susceptible to failure from the effects of water hammer, creep, erosion, corrosion, fatigue, and environmental conditions, as indicated by operating experience. The rationale is that these degradation mechanisms challenge the assumptions in the LBB acceptance criteria. For example, (1) water hammers may introduce excessive dynamic loads which are not accounted for in the LBB analyses, and (2) corrosion and fatigue may introduce cracks whose geometry may not be bounded by the postulated through-wall flaw in the LBB analyses. Adhering to the "defense-in-depth" principle, piping with these potential degradation mechanisms is excluded from LBB applications.

To demonstrate that the candidate piping is not susceptible to failure from these degradation mechanisms, the operating history and measures to prevent or mitigate these mechanisms must be reviewed. An example is the application of LBB to the pressurizer surge line which has been recently observed to have thermal stratification. Because thermal stratification loads were not considered in the original design of the pressurizer surge line and preliminary analyses indicated that the resulting thermal fatigue might be a concern, the utilities were requested to reevaluate the integrity of the surge line with thermal stratification according to ASME Code Section III requirements. Although some surge line supports had to be modified, a number of utilities have completed the reevaluation and found their surge lines to have acceptable integrity, i.e., thermal fatigue failure of the Code-acceptable surge lines was unlikely; the application of LBB was thus permitted.

Piping in boiling water reactors (BWRs) has a history of intergranular stress corrosion cracking (IGSCC). Although the broad scope rule permits the application of LBB in BWRs provided the piping is treated by two IGSCC mitigating measures, the NRC staff is continuing to evaluate the effectiveness of various IGSCC mitigating measures. Because of the IGSCC concern, LBB has not yet been applied to BWR piping.

"Leak-Before-Break" Acceptance Criteria

After the LBB candidate piping has been reviewed for degradation mechanisms as discussed previously and found to be acceptable, the piping is subjected to a rigorous fracture mechanics evaluation. The acceptance criteria established by the NRC are as follows:

- (1) The loading conditions should include the static forces and moments (pressure, deadweight, and thermal expansion) due to normal operation, and the forces and moments associated with the safe shutdown earthquake (SSE). These forces and moments should be located where the highest stresses, coincident with the poorest material properties, are induced for base materials, weldments, and safe ends.

- (2) A through-wall flaw should be postulated at the locations determined from criterion (1) above. The size of the flaw should be large enough so that the leakage from this postulated flaw is assured of detection. When the pipe is subjected to normal operational loads, it should be demonstrated that there is a margin of at least 10 between the leakage from the leakage-size flaw and the plant's installed leak detection capability. The margin on leakage is required to account for uncertainties such as the crack opening area, crack surface roughness, two-phase flow, and leak detection capability.
- (3) It should be demonstrated that the postulated leakage-size flaw is stable under normal plus SSE loads. It should be demonstrated that there is a margin of at least 1.4 between the loads that will cause flaw instability and the normal plus SSE loads. The margin of 1.4 stems from the factor of the square root of two on stress intensity for flaw evaluation under Level D loadings in IWB-3600 of Section XI of the ASME Code. However, the broad scope rule permits a reduction of the margin of 1.4 to 1.0 if the individual normal and seismic (pressure, deadweight, thermal expansion, SSE, and seismic anchor motion) loads are summed absolutely.
- (4) The flaw size margin should be determined by comparing the leakage-size flaw to the critical-size flaw. Under normal plus SSE loads, it should be demonstrated that there is a margin of at least 2 between the critical-size flaw and the leakage-size flaw. The margin of 2 stems from the equivalent factor of the square root of two on stress intensity for flaw evaluation under Level D loadings in IWB-3600 of Section XI.

Status of "Leak-Before-Break" Regulatory Development

The NRC staff is developing Standard Review Plan (SRP) 3.6.3 for the implementation of LBB. The draft SRP 3.6.3 has been published for public comment and the staff is developing the final version of SRP 3.6.3. In the final SRP 3.6.3, the NRC staff will likely propose a fracture mechanics evaluation method based on IWB-3640 and IWB-3650 of Section XI for austenitic and ferritic steels, respectively. Thermal aging of cast stainless steel will be evaluated on a case-by-case basis.

Status of "Leak-Before-Break" Applications

Currently, approximately two-thirds of the PWRs in the U.S. have approval for the application of LBB in the primary coolant loop. (See Table 1 for a list of plant names.) There are also four PWRs which have LBB approval for their auxiliary lines, specifically, pressurizer surge, accumulator, and residual heat removal (RHR) lines. (One of the four plants also has approval for the safety injection lines and the reactor coolant loop bypass lines.) The approved auxiliary lines are all inside containment, fabricated from austenitic stainless steel, and at least 6 inches in diameter. The application of LBB has not been approved for any BWR. Review of LBB applications in the primary loop piping and auxiliary piping is continuing.

Three PWRs have redesigned certain supports (e.g., snubbers) for the primary loop after the elimination of the dynamic loads resulting from postulated pipe ruptures with LBB technology. In these cases, the number of large-bore snubbers is greatly reduced and the risk associated with snubber failure is

reduced accordingly. Figures 2 and 3 show the result of a typical snubber reduction program for one loop of a PWR primary loop piping. Figure 2 shows two out of four snubbers were eliminated from the steam generator (SG) upper support. Figure 3 shows six out of eight snubbers were eliminated from the SG lower support and the reactor coolant (RC) pump support structure.

Conclusions

The DEGB concept has provided a reasonable upper-bound of the global effects of postulated accidents for the design basis of nuclear plants. However, the DEGB concept evolved to be treated as a design basis event, requiring protection from the local dynamic effects of a postulated DEGB. The development of LBB technology and the subsequent GDC-4 rule change have permitted the demonstration of piping integrity by analyses, resulting in the elimination of local dynamic effects of postulated pipe ruptures from the design basis of nuclear power plants. The application of LBB provides an alternative to the installation of potentially counter productive hardware such as pipe whip restraints, jet impingement shields, and snubbers. The licensing evaluation of LBB requires an extensive review based on piping degradation limitations and rigorous fracture mechanics acceptance criteria.

Table 1 Alphabetical List of U.S. Plants with Approved
"Leak-Before-Break" Primary Loop Piping

Beaver Valley 1 and 2
Braidwood 1 and 2
Byron 1 and 2
Callaway 1
Catawba 1 and 2
Comanche Peak 1 and 2
Cook 1 and 2
Ginna
Harris 1
Indian Point 2 and 3
Kewaunee
McGuire 1 and 2
Millstone 3
North Anna 1 and 2
Palo Verde 1, 2 and 3
Point Beach 1 and 2
Prairie Island 1 and 2
Seabrook 1 and 2
South Texas 1 and 2
Surry 1 and 2
THI 1
Trojan
Turkey Point 3 and 4
Vogtle 1 and 2
WNP 1
Wolf Creek 1
Yankee Rowe

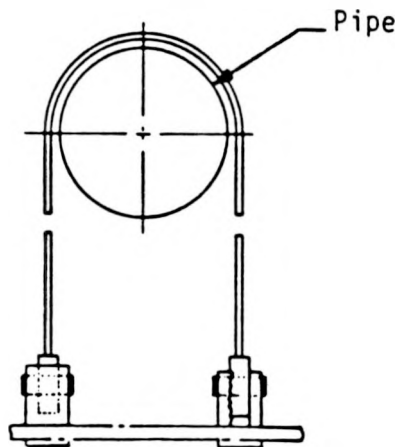
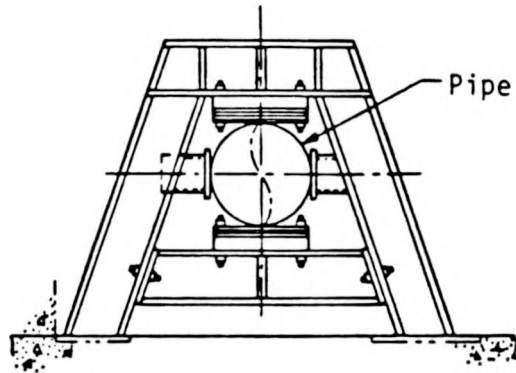
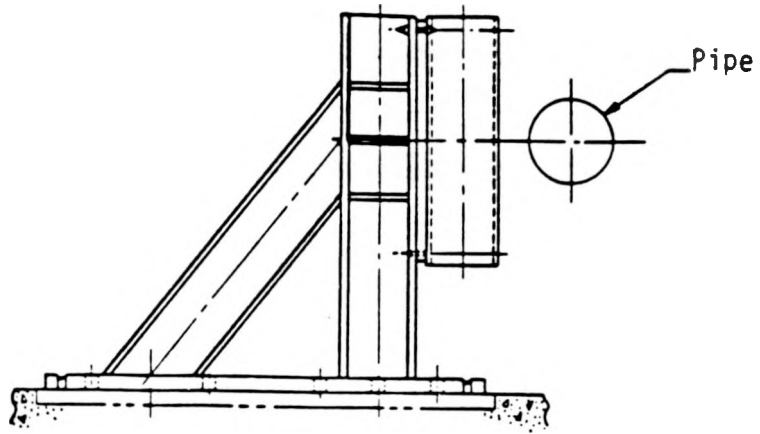


Figure 1 Three typical pipe whip restraint designs.

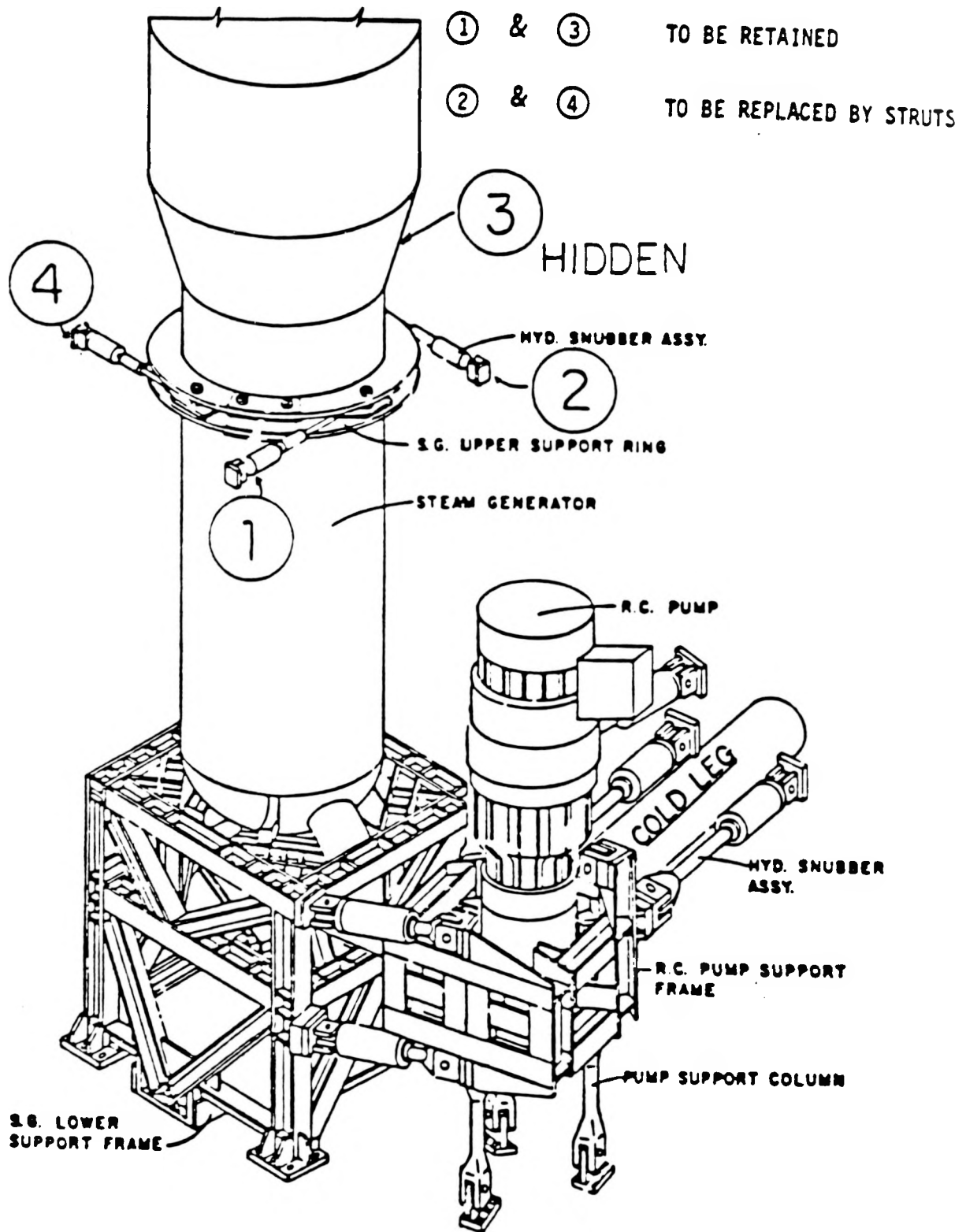


Figure 2 Schematic illustrating typical reduction of snubbers on steam generator upper support based on leak-before-break.

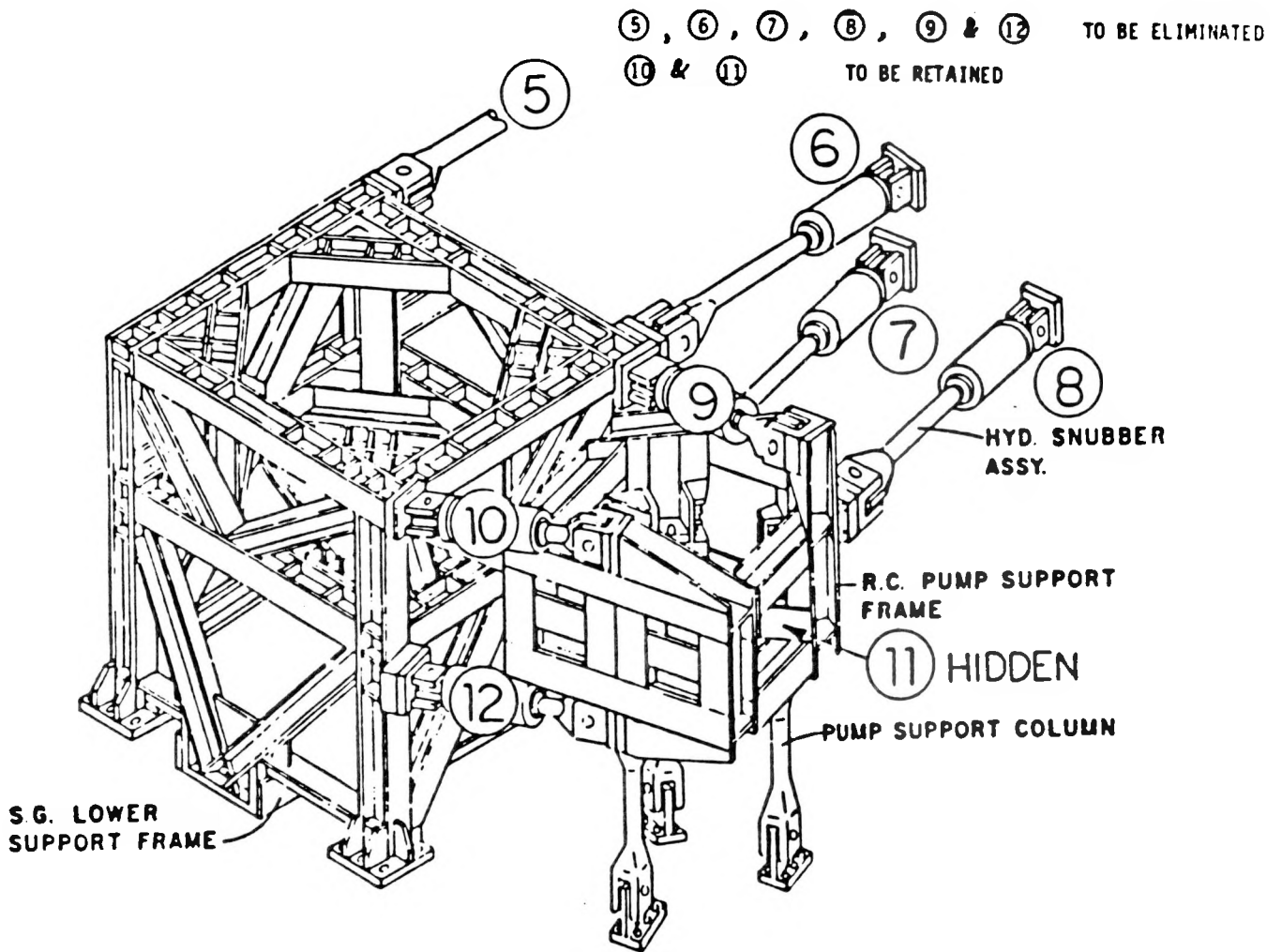


Figure 3 Schematic illustrating typical reduction of snubbers on steam generator lower support and reactor coolant pump support structure based on leak-before-break.

ACCEPTANCE CRITERIA FOR STRUCTURAL
EVALUATION OF EROSION-CORROSION THINNING
IN CARBON STEEL PIPING

D. M. Norris
Electric Power Research Institute (1)

ABSTRACT

This paper is an overview of proposed American Society of Mechanical Engineers Boiler and Pressure Vessel Code evaluation and acceptance criteria for pipe wall thinning caused by erosion or corrosion. The criteria are based on unique application of existing design code requirements. This paper gives the proposed evaluation equations, the application methodology, and an example to assess benefit. The work is currently under consideration by ASME Section XI.

INTRODUCTION

Aging of nuclear power plants and piping failures due to erosion and corrosion have lead to extensive efforts by the industry to better understand wall thinning, to identify potentially susceptible locations, to perform inspections, to evaluate the damage to decide component fitness for continued service, and to repair or replace damaged material if necessary. Background information is given in references [1,2,3,4].

The evaluation criteria described in this paper were developed by the Electric Power Research Institute (EPRI) [5]. For the most part, these criteria are based on piping design-code requirements. While thinning due to erosion or corrosion is not explicitly addressed in these Codes, design rules are available that address geometry and loading conditions which are typical of wall thinning. We used what we felt to be reasonable and appropriate guidance for the thinning problem from these rules. In some cases, we recommended application of rules taken from one Code to a different one.

Subsequent to the EPRI effort, Section XI of the ASME Boiler and Pressure Vessel Code [6] established a working group to prepare rules for erosion inspection and damage evaluation and acceptance criteria. These latter criteria made use of the EPRI results but include more conservative and simplifying assumptions to make them appropriate for ASME Code application.

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SCREENS FOR ACCEPTABILITY

The logic diagram of Fig. 1 describes the evaluation methodology. Fig. 1 uses a three-step screening process: (1) accept or reject thinning based on the ratio of the measured thickness to nominal thickness, or (2) for measured thickness falling between this accept-reject criteria, compare the measured thickness with the Code allowable minimum wall thickness (t_{min}): accept (with caveats) if thicker; evaluate further if the thickness is below t_{min} , and (3), if evaluation is indicated, perform the evaluation using options based on the extent and depth of local thinning.

The thinning geometry is shown in Fig. 2. The proposed Code language defines a minimum predicted wall thickness, t_p , as the measured minimum wall thickness less the predicted thinning occurring over the evaluation service period. Guidance on erosion prediction is found in reference [2].

Screen 1 - Nominal Wall Thickness

Thickness, t_p , is first compared with nominal wall thickness, t_{nom} , available from design drawings, purchase specifications, or material certifications. General requirements for carbon and alloy steel pipe specify permissible variations in wall thickness. For seamless and welded product, the wall thickness may be less than the nominal thickness by up to 12.5%. If t_p is within the specified wall thickness tolerance, the component is acceptable. If t_p is less than 30% of the nominal thickness, repair or replacement is required.

For cases where the predicted wall thickness is less than nominal wall thickness minus the manufacturing tolerance - but greater than 30% of the nominal wall thickness, more detailed evaluation is required.

Screen 2 - Minimum Wall Thickness

This is calculated using the Code of construction, material mechanical properties, loads, and an allowance for erosion. If t_p is below manufacturing tolerance, but greater than t_{min} , no further evaluation is required but monitoring is recommended. If t_p is less than t_{min} , further evaluation is required. Calculation of t_{min} is somewhat complicated by the need to satisfy bending as well as the pressure loads [5,6,7]. More details are given in the appendix.

Screen 3 - Local Thinning Options

When the extent of thinning is limited and the resulting stress magnification can be demonstrated to be local, it may be possible to maintain Code safety margins even if t_p is less than t_{min} .

Three options are available for local wall thinning: (1) erosion criteria developed for evaluation of gas pipelines, (2) an option

based on piping branch reinforcement design rules, and (3), options for local primary membrane stress that exceed the design stress. The condition to be satisfied is

$$t_p \geq t_{aloc} \quad (1)$$

where t_{aloc} is the allowable local wall thickness. If it is not possible to satisfy these local stress options, repair or replacement is required.

THE LOCAL WALL THINNING OPTIONS

We discuss here the three local thinning options discussed above. Fig. 3 shows the range of applicability of the options in a plot of t_{aloc}/t_{min} versus $L_{ma} / \sqrt{R \cdot t_{min}}$. The branch reinforcement option provides some margin intermediate between the two curves of Fig. 3. These options apply if the minimum wall thickness requirements cannot be met.

Application of ANSI/ASME B31G to Axial Corrosion

Appendix B31G to the ANSI/ASME B31.1 Code [8] provides guidance for determining the strength of corroded pipe lines based on the work of Kiefner and Duffy for the American Gas Association [9,10]. Kiefner conducted experiments with corroded pipe and demonstrated that the burst pressure could be predicted (for ferritic and austenitic pipe) by simple empirical expressions. They found that the amount of thinning in the circumferential direction did not change the limiting burst pressure. The tests used only pressure loading.

For the case of nuclear plant piping, where bending stresses may be significant, the extent of circumferential thinning must be limited for the criteria to be applicable. We recommended limiting application of this criteria by $L_{mt} \leq \sqrt{R \cdot t_{min}}$.

Curve 1 in Fig. 3 is used to determine the local allowable wall thickness, t_{aloc} , as a function of axial extent of local thinning, L_{ma} , below t_{min} .

Application of Branch Reinforcement Rules

Versions of nuclear design Codes provide rules for design of branch connections. The rules compensate for material lost in the branch opening such that overall structural capacity of the pipe is maintained. These rules include the amount of material to be added and its location and distribution. Cases where no reinforcement is needed are also defined.

Figure 4 shows how branch reinforcement rules can be used to evaluate local thinning. The area associated with this loss is

$$A1 = Lm * (tmin - tp) \quad (3)$$

Branch reinforcement rules require that lost area be compensated with excess material distributed uniformly around the periphery of the loss region, within certain limits of reinforcement, L_a . This material is generally available because the pipe thickness is greater than t_{min} . The replacement area is

$$A2 = (L_a - L) * (t_{nom} - t_{min}) \quad (4)$$

Using eqs. (3) and (4), and the requirement that structural integrity of the material over the thinned region must be maintained [5], the local allowable pipe wall thickness, t_{aloc} , can be determined as follows.

If

$$Lm \leq 2.65 \sqrt{R * t_{min}} \quad (5)$$

and

$$t_{nom} > 1.13 * t_{min}, \quad (6)$$

then use the larger of

$$t_{aloc}/t_{min} \geq 1 - 1.5 * \sqrt{R * t_{min}} * (t_{nom}/t_{min} - 1)/L \quad (7)$$

or

$$t_{aloc}/t_{min} \geq 0.353 Lm / \sqrt{R * t_{min}}. \quad (8)$$

Local Membrane Stress

The t_{min} equations in the various codes depend on membrane stress limits that assume uniform pipe thickness. For cases in which the thickness is reduced over a local region, a less restrictive

requirement is appropriate. Although piping design codes do not specifically address such local thickness effects, ASME Section III, Class 1 piping design rules permit use of alternative rules for design by analysis. These rules allow a local primary membrane stress limit of $1.5 S_m$ provided that the stress does not exceed $1.1 S_m$ over a distance $\sqrt{R} \cdot t_{min}$. S_m is the design stress [6]. This limit is shown in Figure 5.

With the conservative assumption that thinning extends completely around the pipe, we used thin shell equations and finite element analysis to define an acceptable thinning depth and axial extent [5] for internal pressure loading. This analysis conservatively ignored contribution of material exceeding t_{min} . The result is given in Fig. 3 as curve 2. It is necessary to also satisfy equations that limit the primary axial stresses. The applicable equations are given in the various codes.

It may be shown [5] that these axial stress equations limit the allowable thickness predicted in Fig. 3 at values of $L_{ma} / \sqrt{R} t_{min}$ below about 2. The appropriate t_{aloc} is that obtained from Fig. 3 subject to the conditions given below.

For ASME Section III and B31.7, Class 1 piping systems

$$t_{alco}/t_{min} \geq [B1 + (t_{nom}/t_{min}) \cdot (P_b/S_m)]/1.5, \quad (9)$$

for ASME Section III, Classes 2 and 3 piping systems

$$t_{aloc}/t_{min} \geq [B1 + (t_{nom}/t_{min}) \cdot (P_b/S_m)]/1.8, \quad (10)$$

and for B31.7 Classes 2 and 3, and B31.1 piping systems

$$t_{aloc}/t_{min} \geq [0.5 + (t_{nom}/t_{min}) \cdot (P_b/S)]/1.2. \quad (11)$$

A pointing algorithm given in Fig. 6 selects the appropriate local option.

A BENEFITS ASSESSMENT FOR LOCALIZED THINNING

To find the benefit to be derived from these local options (and to check the pointing algorithm of Fig. 6) we calculated ratios of t_{aloc}/t_{min} for a particular piping component designed to the three construction codes. These allowable thinning values are given in

Table I, for axial thinning to $8\sqrt{R \cdot t_{min}}$ and circumferential thinning to $4\sqrt{R \cdot t_{min}}$.

For this test calculation we assumed a 24-inch diameter, SA106C Schedule 80 wrought carbon steel pipe. We used a design pressure of 1250 psi and assumed P_m and P_b both equal to 6 ksi. The nominal thickness for this pipe is 1.218 inches and t_{min} was calculated to be 0.8051 for the Class 1 Construction Codes and 0.8958 inches for the other two Codes.

The calculation assumed a thinned rectangular area defined by lengths L_{mt} and L_{ma} (L_m is then easily calculated from the definition). L was chosen arbitrarily as two times L_m (see Fig 2).

To facilitate the discussion we will refer to Table I entries in terms of normalized L_{ma} and L_{mt} defined as L_{ma}' and L_{mt}' . $L_{mt}' = 3$ means $L_{mt} = 3 \cdot \sqrt{R \cdot t_{min}}$. Table Ia entry, "(2,1)" for (L_{mt}', L_{ma}') , will be understood to point to the value of t_{loc}/t_{min} of 0.828.

Benefits Discussion

Table I shows that substantial margins exist below t_{min} . Benefit is derived (in this example) for 360 degree transverse erosion and axial erosion below t_{min} to about 2 feet in length. Small spots with thicknesses less than half of t_{min} maintain original code design margins.

Table I uses Appendix B31G for transverse thinning where $L_{mt}' \leq 1$ in the first two rows of Tables I a,b,c. For greater transverse thinning B31G is not applicable and the algorithm of Fig. 6 indicates the branch reinforcement option. Application of these equations provides margin but is limited to small areas where $L_m \leq 2.65\sqrt{R \cdot t_{min}}$ shown in the "boxed" regions in Table I.

The local membrane stress equations are used in rows below $L_{mt}'=1$ except in the boxed regions. In general, the local membrane stress is computed from curve 2 in Fig. 3 except where limited by the bending stress given in Eqs. 9,10, and 11. For this example with limited bending stress, only values in Table Ia greater than $L_{mt}'=2.5$ for L_{ma}' of 0.5 are affected. Similar limits are imposed at L_{ma}' below 1.5 in Table Ic.

We used Eqs. 9,10, and 11 to compute minimum t_{loc}/t_{min} values for various bending to design stress ratios in Table II (for this 24-inch diameter schedule 80 SA106C pipe). For the bending-to-design stress ratios used in this example the limiting thinning is given and can be compared with the values computed given in Table I.

In this example, we found that more margin is generally available than that computed by the branch reinforcement option (in the boxes of Table I) if the local membrane stress equations are used. For

example, only the t_{loc}/t_{min} values in Table Ia at (1.5,1), (1.5,1.5), and (1.5,2) provide more margin than curve 2 of Fig. 3.

The benefit from the local membrane stress option is dependent on the excess pipe thickness available over t_{min} . In this example, because of the differences in design stress between the three codes, the higher ratio of t_{nom} to t_{min} equal to 1.513 for the Class I piping provides more benefit than the ratio of 1.360 for the other two codes.

In the original work(5) we recommended that each of the local thinning options be computed independently and the one providing the most margin selected for application. It was decided by the code committee that this was inappropriate for code criteria and that specific guidance should be given that resulted in the pointing algorithm of Fig. 6. As shown above in this single benefit example, the algorithm always leads to a conservative but not optimum result.

SUMMARY

We discussed a three-step screening evaluation of erosion/corrosion thinning in carbon steel piping under consideration by the ASME Code. The screens included a screen based on nominal thickness allowables to determine if further evaluation is warranted, a screen based on comparison of thinning with design minimum wall thickness requirements, and a screen for local thinning less than the design minimum thickness.

For the case of the local options we gave results of calculations giving the range of margin that exists below Code minimum wall thicknesses.

The codification of these evaluation and acceptance criteria has in some instances required simplifications that were necessarily conservative. Additional margins may be available if evaluations are based on the original thinning criteria [5]. Software for these calculations and those methods of [5] is available [11].

ACKNOWLEDGEMENT

This work was supported by the Electric Power Research Institute Research Project 1757-61. This paper is an expansion of an earlier paper [7] and we acknowledge the contributions of the other authors. The principal developer was Dr. T. L. Gerber, of the Structural Integrity Associates. We thank the members of the ASME, Section XI, Special Working Group on Pipe Wall Thinning (Mr. John P. Houstrup was the principal author of our section of the Code document) for their suggestions during the application of this technology to the Code.

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Appendix

MINIMUM WALL THICKNESS

Design rules for nuclear power plant piping are given in various Code documents. The rules depend on applicable Construction Code, the date of construction, safety classification, and other licensing commitments.

The governing equation is of the form

$$t_{min} = P \cdot D_o / [2(S + yP) + A] \quad (1A)$$

where P is the design pressure, D_o the outside pipe diameter, S (or S_m) is a code specific design stress, $y = 0.4$, and A is a thinning allowance (for the calculations reported here it was taken as 1/16 inch). The design codes also put restrictions on axial stress, and these restrictions may occasionally control t_{min} . If the axial stress requirements can not be satisfied, t_{min} must be increased until the axial stress requirements are met [5].

TABLE I (1,2)

Allowable normalized local thickness (t_{loc}/t_{min}) as a function of normalized axial (L_{a1}) and circumferential (L_{c1}) thinning length

Table Ia - Section III and B31.7, Class 1 (3)

L_{c1} (4)	L_{a1} (5)										
	0.5	1	1.5	2	2.5	3	4	5	6	7	8
0.5	0.300	0.411	0.599	0.687	0.735	0.763	0.795	0.846	0.898	0.949	1.000
1.0	0.300	0.411	0.599	0.687	0.735	0.763	0.795	0.846	0.898	0.949	1.000
1.5	0.757	0.787	0.819	0.882	0.950	0.954	0.963	0.973	0.982	0.991	
2.0	0.813	0.828	0.822	0.883	0.950	0.954	0.963	0.973	0.982	0.991	
2.5	0.900	0.799	0.855	0.883							
3.0	0.641	0.799	0.855								
3.5	0.641										
4.0											

Table Ib - ASME Section III Classes 2 and 3 (6)

L_{c1}	L_{a1}										
	0.5	1	1.5	2	2.5	3	4	5	6	7	8
0.5	0.300	0.411	0.599	0.687	0.735	0.763	0.795	0.846	0.898	0.949	1.000
1.0	0.300	0.411	0.599	0.687	0.735	0.763	0.795	0.846	0.898	0.949	1.000
1.5	0.829	0.850	0.873	0.892	0.950	0.954	0.963	0.973	0.982	0.991	
2.0	0.869	0.879	0.892	0.883	0.950	0.954	0.963	0.973	0.982	0.991	
2.5	0.900	0.799	0.855	0.883							
3.0	0.632	0.799	0.855								
3.5	0.632										
4.0											

Table Ic - B31.7 Classes 2 and 3, B31.1 (6)

L_{c1}	L_{a1}										
	0.5	1	1.5	2	2.5	3	4	5	6	7	8
0.5	0.300	0.411	0.599	0.687	0.735	0.763	0.795	0.846	0.898	0.949	1.000
1.0	0.300	0.411	0.599	0.687	0.735	0.763	0.795	0.846	0.898	0.949	1.000
1.5	0.829	0.850	0.873	0.892	0.950	0.954	0.963	0.973	0.982	0.991	
2.0	0.869	0.879	0.892	0.883	0.950	0.954	0.963	0.973	0.982	0.991	
2.5	0.900	0.805	0.855	0.883							
3.0	0.805	0.805	0.855								
3.5	0.805										
4.0											

- (1) SA 106C 24-inch Schedule 80 wrought carbon steel pipe
- (2) $P_m = P_b = 6 \text{ Ksi}$, 500F
- (3) $t_{min} = 0.805 \text{ in.}$
- (4) Normalized circumferential thinning length, $L_{c1} = L_{c1} / \sqrt{R \cdot t_{min}}$, $\sqrt{R \cdot t_{min}} = 3.11 \text{ in}$
- (5) Normalized axial thinning length, $L_{a1} = L_{a1} / \sqrt{R \cdot t_{min}}$, $\sqrt{R \cdot t_{min}} = 3.28 \text{ in.}$
- (6) $t_{min} = 0.896 \text{ in.}$

TABLE II (1)

Limiting values of $\sigma_{loc}/t\epsilon_{min}$ for local membrane stress option
 computed from Eqs. 9, 10, and 11 showing the effect of bending.

Eq	$\sigma_{nom}/t\epsilon_{min}$	P_b/S						
		0.000	0.200	0.305(2)	0.343(3)	0.400	0.500	0.800
9	1.513	0.333	0.535	0.641		0.737	0.939	1.000
10	1.360	0.278	0.429		0.537	0.580	0.731	0.882
11	1.360	0.417	0.643		0.805	0.870	1.000	1.000

(1) Applies only to 24-in diam SA 106C pipe

(2) Ratio for example ($S_m = 19700$ psi, $P_b = 6000$ psi, $T = 600F$)

(3) Ratio for example ($S = 17500$ psi, $P_b = 6000$ psi, $T = 600F$)

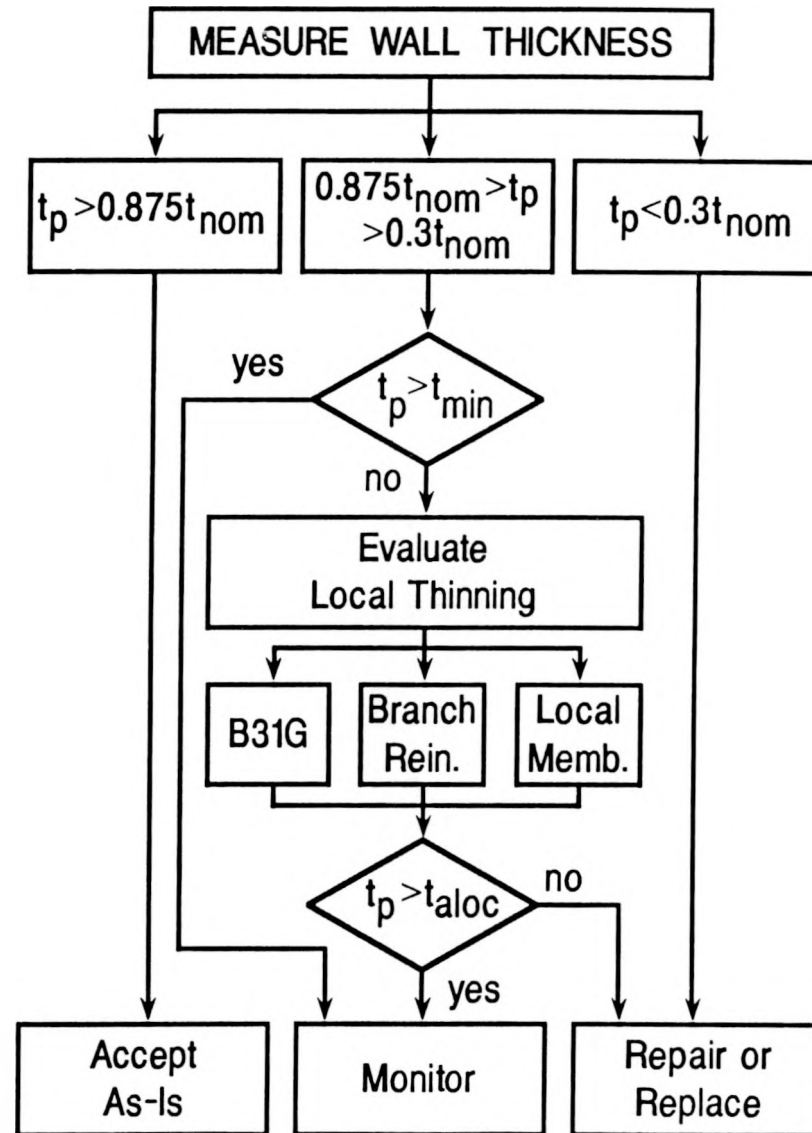


Figure 1. Evaluation of wall thinning logic diagram.

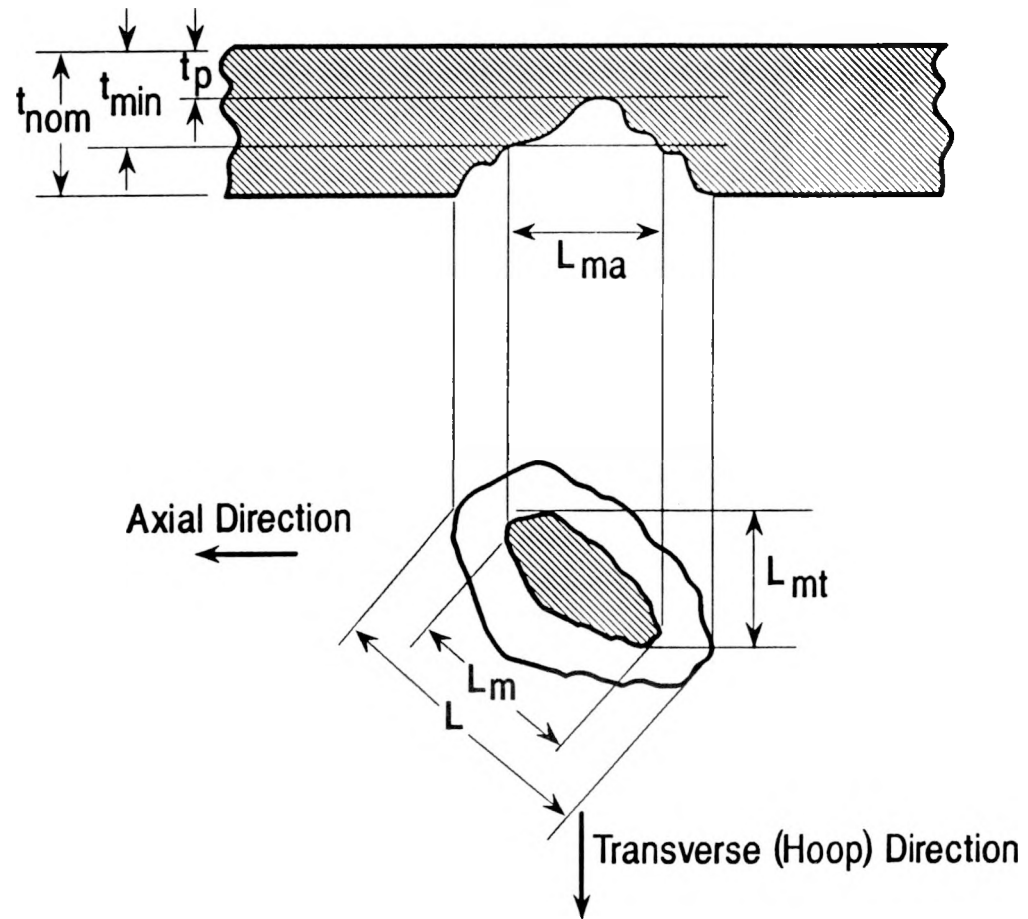


Figure 2. Schematic of depth and extent of wall thinning.

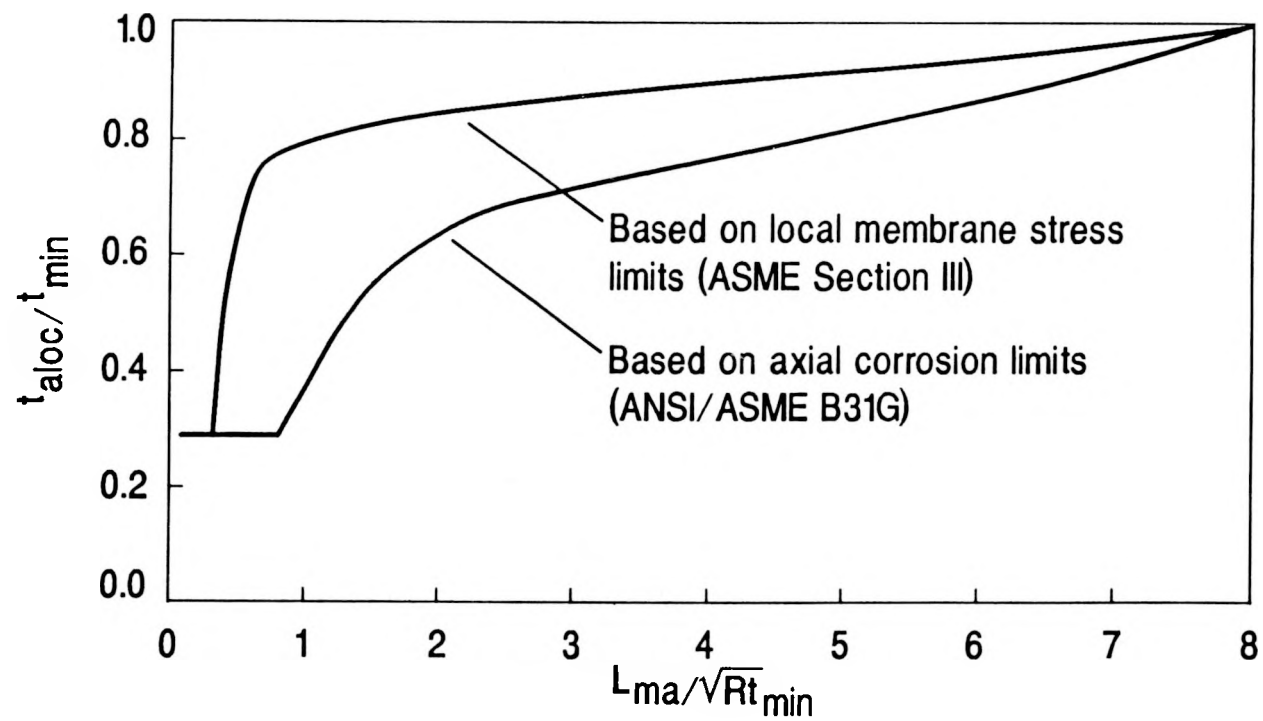


Figure 3. Allowable depth and length of locally thinned area.

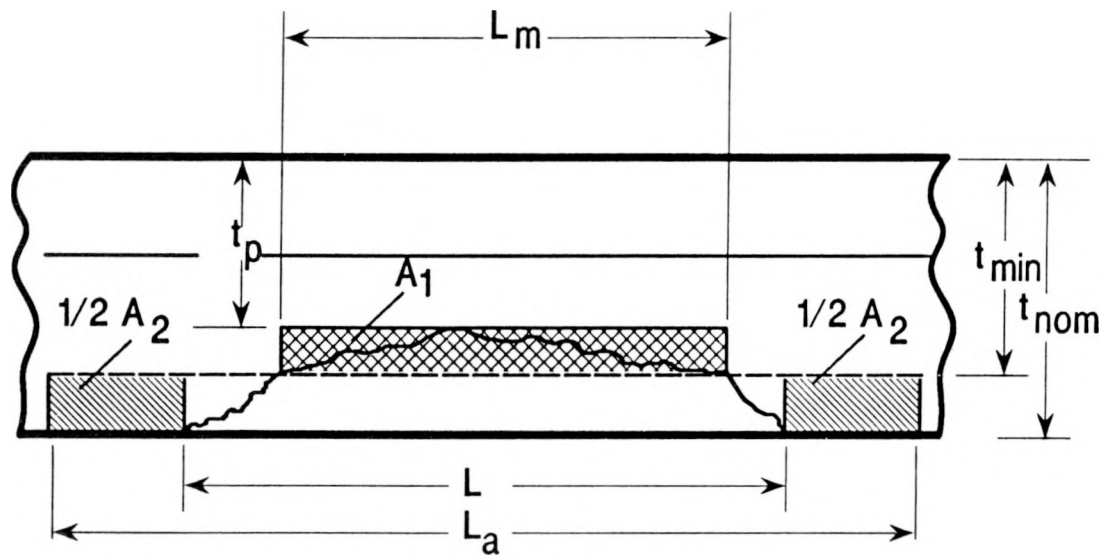


Figure 4. Concept of area reinforcement for local thinning wall loss.

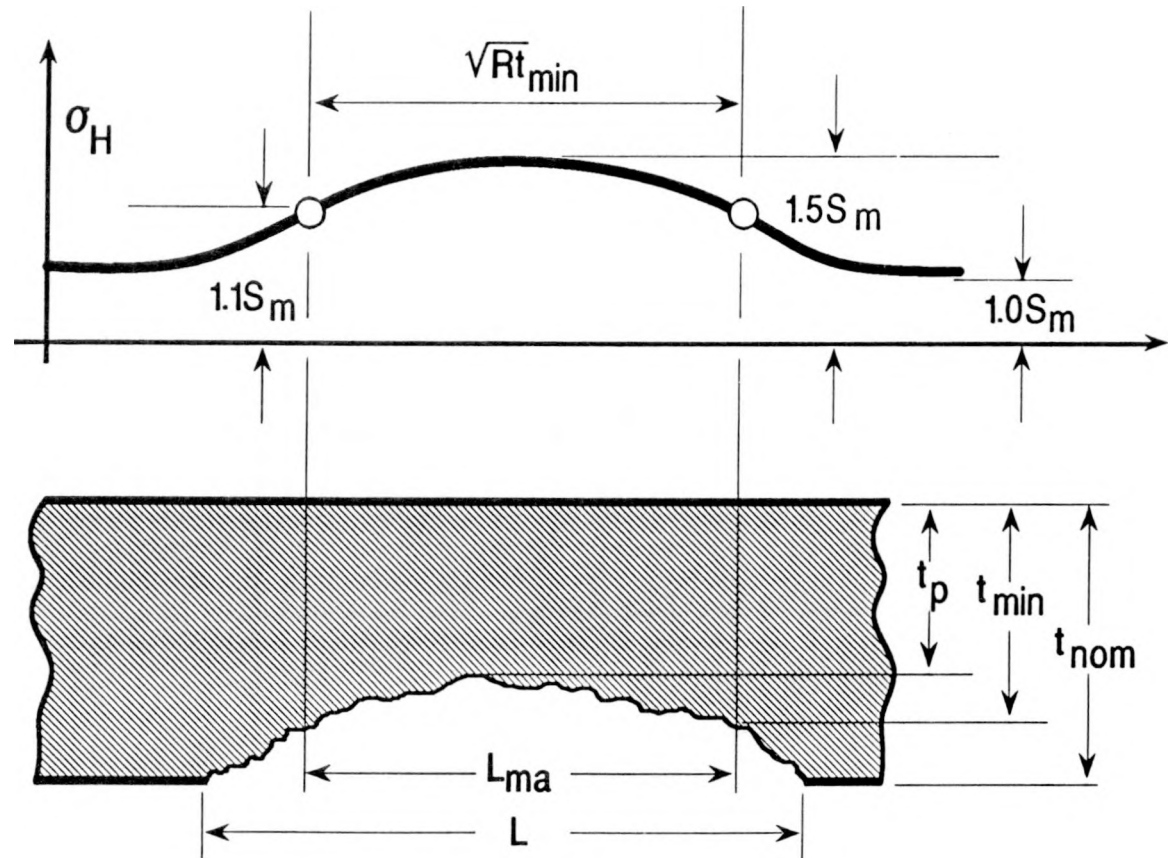


Figure 5. Definition and stress limits for local membrane stress per ASME Section III, NB-3200.

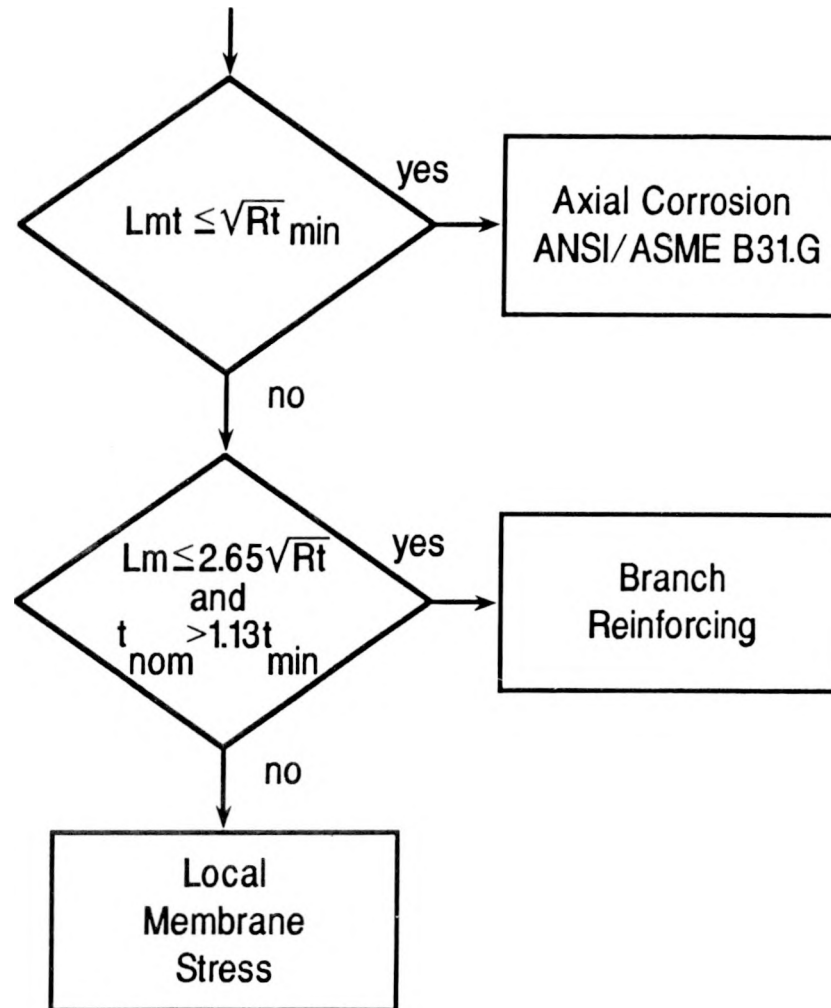


Figure 6. Local wall thinning selection algorithm.

SESSION 2

CO-CHAIRMEN

**DR. K. TING
INER, TAIWAN**

**DR. K. BRICKSTAD
SKI, SWEDEN**

LEAK BEFORE BREAK IN FRENCH NUCLEAR POWER PLANTS

C. FAIDY
EDF-SEPTEN

S. BHANDARI
FRAMATOME

P. JAMET
CEA

Practical applications of the leak before break concept at the present stage are quite limited in French nuclear power plants. However, discussions are always carried out with Safety Authorities for the different types of reactors : Pressurized Water Reactor (PWR), Liquid Metal Fast Breeder Reactor (LMFBR) and Gas Graphite Reactor (GGR).

Presently, the fracture mechanics part of the studies are complete for different components :

- pipes en GGR,
- primary and auxiliary lines in PWR,
- steam generator tubes in PWR,
- pipes and main vessel in LMFBR.

The different approaches are consistent but some specific problems have to be taken into account plant by plant like : creep regime, thin shell components, in-service inspection or design safety issue...

A large Research and Development program realised in different cooperative agreements (national or international) completes the general approaches with respect to different topics like material properties, elasto-plastic fracture mechanics, leak area determination and leak detection devices.

In conclusion, although practical applications are presently limited, but EDF is determined, jointly with FRAMATOME, NOVATOME and CEA to define a complete validated methodology usable, as a first step, on different specific cases.

FRENCH L.B.B. PROGRAM

C. FAIDY - S. BHANDARI - P. JAMET

EDF FRAMATOME CEA

- PRACTICAL APPLICATIONS

- STATIC TESTS

- ANALYTICAL STUDIES

F.E.M.

ENGINEERING

- DYNAMIC ASPECTS

- INTERNATIONAL PROGRAMS

- CONCLUSIONS

PRACTICAL APPLICATIONS

- ALWAYS LIMITED

STEAM GENERATOR TUBES (PWR)

MAIN VESSEL AND PIPES (LMFBR)

PIPES (GGR)

- BUT: EDF, FRAMATOME, AND CEA DEVELOP

A VALIDATED METHODOLOGY

- FEASIBILITY STUDY ON 4 LOOPS FRENCH PWR

THROUGH-WALL CRACKS SPRING 89

PART THROUGH-WALL CRACKS SUMMER 89

COSTS - BENEFITS END 89

SYNTHESIS END 89

QUASI STATIC TESTS

PROTOTYPE:
(20 TESTS)

OUR GEOMETRY
OUR MATERIALS

ANALYTICAL:
(14 TESTS)

VALIDATION OF NUMERIC APPROACHES

LIMIT MOMENT

INITIATION AND STABLE CRACK
GROWTH

EXPERIMENTAL DETERMINATION
OF J

DYNAMIC TESTS

OBJECTIVES:

SEISMIC LOADS IN CRACK REGION

STRESS CLASSIFICATION

VALIDATION ON 1-D CRACKED
ELEMENT

8 TESTS

ON

STRAIGHT PIPES
ELBOWS
NOZZLES

2 OR 3 TESTS

ON

PIPE SYSTEMS TO BE DEFINED AFTER
PREVIOUS TESTS AND IPIRG TESTS

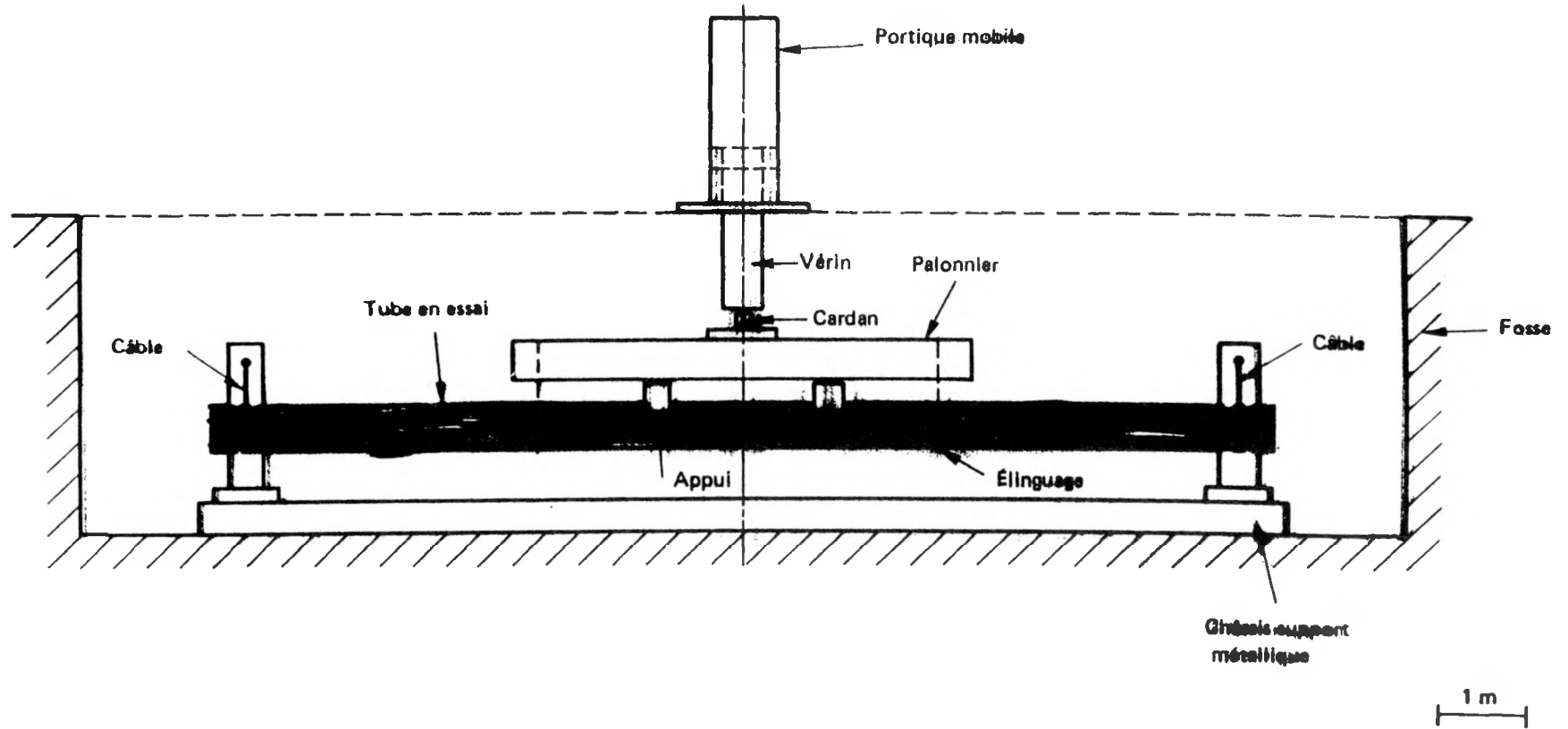


Figure 11 – Le banc d'essai de la DETM.



██████████ - GRILLE D'ESSAIS, TUBE Ø 16" (406,4 mm).

1) ~~acier A36~~ **CARBON STEEL**
 Épaisseur 21 mm **Thickness**

N°	Type défaut	Angle 2α	Profondeur (d/t)	Température (°C)	Chargement	Observations
	Traversant T.V.C.	120°	1	300	Monotone	
	Traversant	60°	1	300	Monotone	
	Traversant	120°	1	300	Monotone	Joint soudé. Weld
4	Surface, interne Int. Surf. Cr.	120°	0,08	300	Monotone	Usinage par électro-érosion. Electro eroded defect
	Traversant	30°	1	300	Monotone	Joint soudé. Weld
	Traversant	30°	1	300	Type (séisme)	

2) Acier Z 3 CN D 17.12 Azote contrôlé **STAINLESS STEEL**
 Épaisseur 40,5 mm **Thickness**

N°	Type défaut	Angle 2α	Profondeur (d/t)	Température (°C)	Chargement	Observations
	Traversant	40°	1	300	Monotone	
	Traversant	120°	1	300	Monotone	
	Traversant	40°	1	300	Monotone	Joint soudé. Weld
4	Surface, interne S.C.	120°	0,08	300	Monotone	Usinage par électro-érosion. Electro eroded defect



EDF SEPTEN

██████████ - GRILLE D'ESSAIS, TUBE Ø 6" (168,3 mm).

1) Acier TU 48C **CARBON STEEL**
 Épaisseur 11 mm **Thickness**

N°	Type défaut	Angle 2α	Profondeur (d/t)	Température (°C)	Chargement	Observations
1	Sens	-	-	300	Monotone	
2	Traversant T.W.	30°	1	300	Monotone BASIC-Static	
	Traversant	120°	1	300	Monotone	
4	Traversant	30°	1	300	Monotone	Joint soudé
5	Traversant	30°	1	300	Type essimes Cyclic	

2) Acier Z 2 CN 18.10 **STAINLESS STEEL**
 Épaisseur 18,2 mm **Thickness**

N°	Type défaut	Angle 2α	Profondeur (d/t)	Température (°C)	Chargement	Observations
1	Traversant	40°	1	300	Monotone	
2	Traversant	120°	1	300	Monotone	
3	Traversant	40°	1	300	Monotone	Joint soudé Weld
4	Traversant	40°	1	300	Type essimes	

STATUS OF PROTOTYPE TEST PROGRAM

11 TESTS COMPLETED ON 19

REPORT ONLY ON FIRST 8 TESTS

DELAY FOR MATERIAL CHARACTERIZATION

DELAY ON ANALYTICAL WORK

OBJECTIVE: COMPLETE BEFORE END 1989

TABLEAU 2 - GÉOMÉTRIE DES TUBES ET CARACTÉRISTIQUES DU DISPOSITIF DE FLEXION

Numéro	Diamètre extérieur D (mm)	Épaisseur t (mm)	Rayon moyen R_m (mm)	R_m/t	Matériau	Angle total 2θ (°)	Écartement des appuis intérieurs L (m)	Écartement des appuis extérieurs Z (m)	Longueur totale du tube LT (m)
DETN 0	406,4	21	192,7	9,2	A42	90	2	8	9,8
DETN 1	406,4	21	192,7	9,2	A42	120	2	8	12
DETN 2	406,4	21	192,7	9,2	A42	60	2	8	12
DETN 3	406,4	21	192,7	9,2	A42 (joint soudé)	120	2	8	12
DETN 4	406,4	40,5	193,8	4,5	AISI 316L	120	2	10	12
DETN 5	406,4	40,5	193,8	4,5	AISI 316L	40	2	10	12
REN 1	168,8	11,1	78,8	7,1	TU 48C	30	0,7	2	2,3
REN 2	168,3	10,9	78,7	7,2	TU 48C	120	0,7	2	2,3

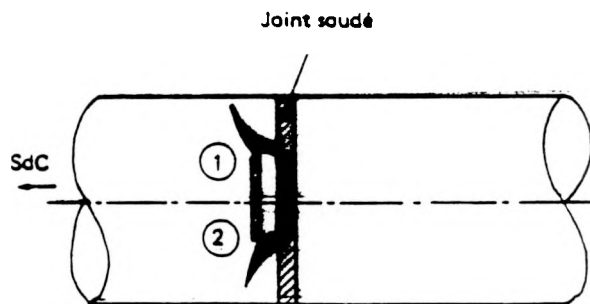
TABLEAU 4 – MOMENT MAXIMUM ET A L'AMORÇAGE – COMPARAISON AVEC LE MOMENT LIMITE CALCULÉ

Numéro de l'essai	Moment maximum				Amorçage			Moment limite M_L (kN.m) (*)	$\frac{M_i}{M_L}$ (%)	$\frac{M_{max}}{M_L}$ (%)
	Force (kN)	Déplacement vérin (mm)	Moment $M_{i,max}$ (kNm)	Angle du défaut (°)	Force (kN)	Déplacement vérin (mm)	Moment M_i (kN.m)			
DETN 0	300	146	486	113	(**)	(**)	(**)	583,4	—	87,2
DETN 1	233,6	141	382	136,4	189	60	286	405,2	72,8	89,3
DETN 2	411,2	286	828,6	77	283	70	486	689	65,2	94,0
DETN 3	242	140	375	132,3	201	65 (***)	313	405,2	77,2	82,6
DETN 4	326,5	325	696,3	132,5	298	150	889	775	82,4	88,8
DETN 5	625	527	1293	52	558	263	1189	1467	79,6	88,7
REN 1	336,1	42	106,2	41	328,3	36	104,1	100,6	103,4	105,6
REN 2	131,8	30	42,0	142,5	105,7	10	34,1	50,5	67,5	83,2

- (*) Moment théorique calculé avec une contrainte d'écoulement σ_f
- (**) Valeur inconnue (pas de suivi du potentiel)
- (***) Estimé d'après les marquages (panne de l'appareil ANG).

**TABLEAU A3.4 - ESSAI DETN 3 ($2\theta = 120^\circ$ joint soudé)
EVOLUTION ANGULAIRE DES DÉFAUTS LORS DES MARQUAGES.**

	M1	M2	M3	M4	M5	M6	Fin	
Déplacement du vérin (mm)	144,5	190,8	232,4	286,5	338,8	386,5	438	
$\Delta\theta$ ($^\circ$) extrémité 1	5,2	13,8	22,6	31,6	37,9	43,4	47,1	(\rightarrow)
$\Delta\theta$ ($^\circ$) extrémité 2	7,9	17,8	25,6	34,7	41,4	47,1	49,8	(\bullet)
θ total ($^\circ$)	133,1	151,6	168,2	186,3	199,3	210,5	216,7	



Repérage des extrémités du défaut

THICKNESS VARIATION $\leq 2 \text{ mm}$
 $\leq 12\%$

INITIATION POINT DIFFICULT TO DETERMINE:

INSIDE WALL

CRACK BLUNTING

SPECIFICALLY FOR CS - SMALL CRACK ANGLE

CRACK PROPAGATIONS COMPLEX

BIFURCATION 20° SS
 60° CS

CHEVRON

COMPLEX CRACK SURFACES

OVALISATION LIMITED AT D/3
 NEGLIGIBLE AT D

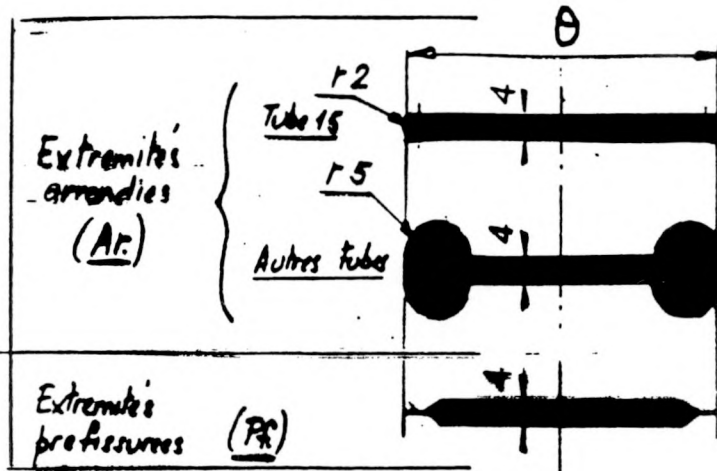
MAX: 14% 16" $2\theta = 120^\circ$ CS

NSCL

M_I/M_1 65 TO 80% 26" 67 TO 100% 6"

M_{max}/M_1 87 TO 94% 26" 83 TO 100% 6"

EXTREMITES D'ENTAILLES

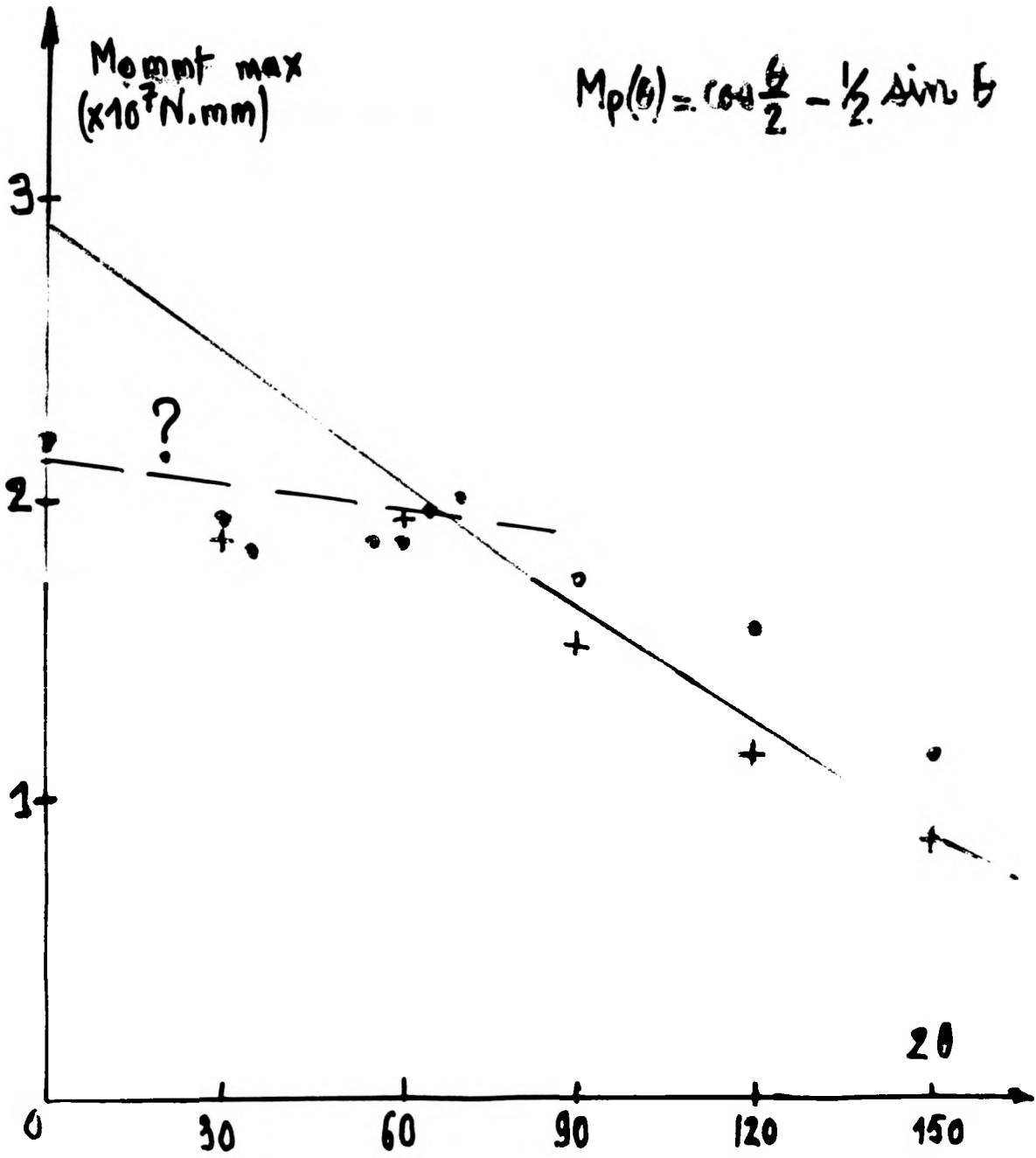


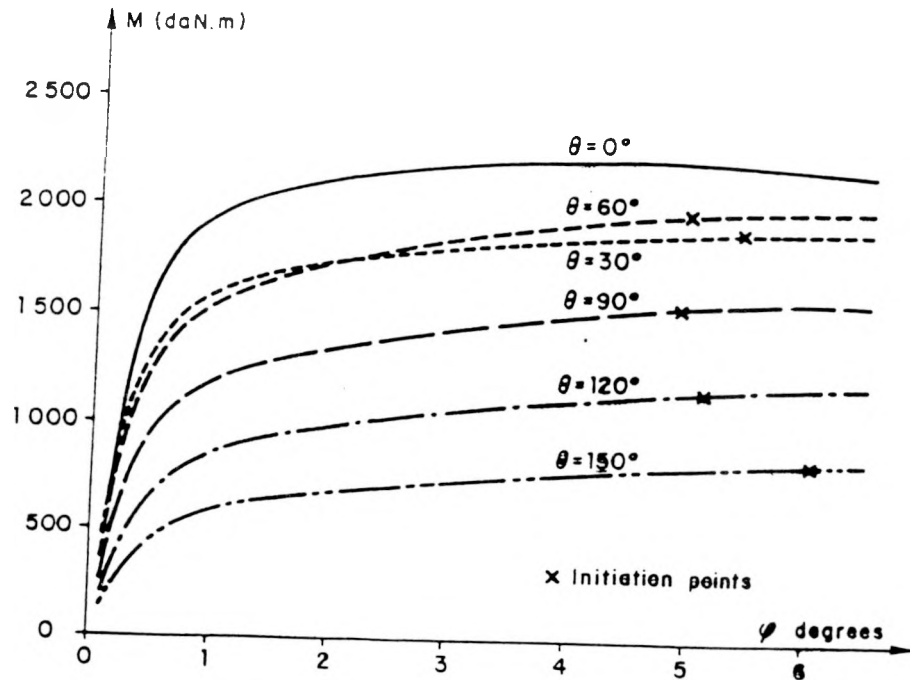
Tube n°	θ degrés	Extremities	Moment en fin de linearité	Moment maxi
9	0	—	$10,5 \cdot 10^6 \text{ N.m}$	$22 \cdot 10^6 \text{ N.m}$
13	30	<u>Ar</u>	9,5 "	19,43 "
12	30	<u>Pf</u>	9 "	18,86 "
14	55	<u>Ar</u>	8,25 "	18,67 "
16	60	<u>Ar</u>	7 "	18,8 "
22	60	<u>Pf</u>	7,5 "	19,8 "
15	65	<u>Ar</u>	8 "	19,91 "
8	70	<u>Ar</u>	8,6 "	20,3 "
21	90	<u>Ar</u>	6,25 "	17,52 "
19	90	<u>Pf</u>	7,2 "	15,43 "
11	120	<u>Ar</u>	5,7 "	15,95 "
5	120	<u>Pf</u>	4,75 "	11,81 "
4	150	<u>Ar</u>	3,5 "	11,58 "
3	150	<u>Pf</u>	3,8 "	8,45 "

$$\phi = 97 \text{ mm}$$

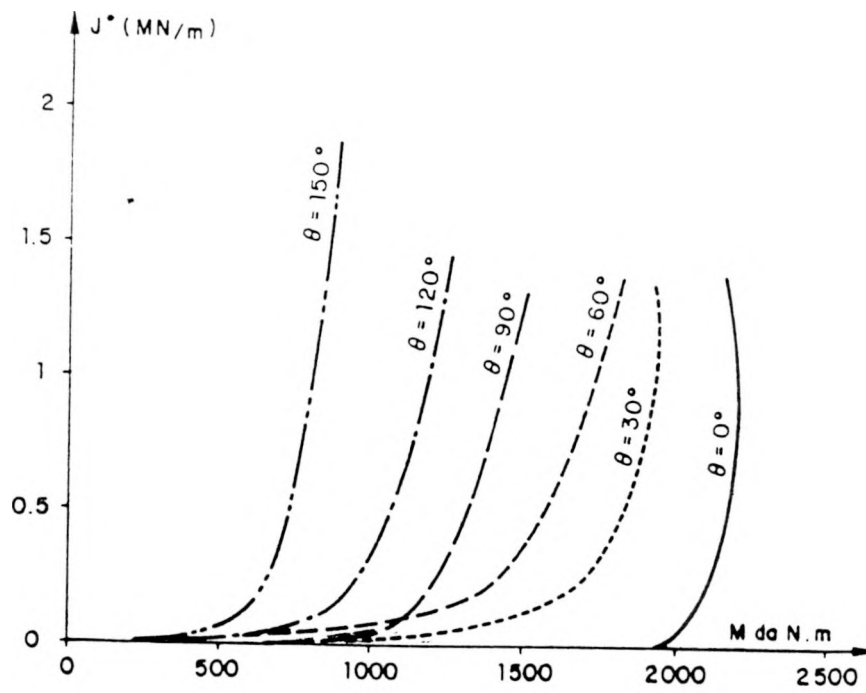
$$t = 8,3 \text{ mm}$$

$$\sigma_c = 370 \text{ MPa}$$





$M-\phi_2$ plots for precracked tubes



J^* estimations for blunted cracked tubes

ANALYTICAL METHODS

F.E.M.

V.C.E

G COMPUTATION

T COMPUTATION

F.E.M. WITH DAMAGE MODELS TO STUDY TRIAXIALITY EFFECTS

1-D CRACKED ELEMENT

ELASTIC

NONLINEARITY

PLASTICITY

CONTACT

CRACK GROWTH

ENGINEERING METHODS

REFERENCE STRESS METHODS LIKE RG

EPRI/GE METHODS

NRC - PIPE

PARIS METHODS

SCREENING CRITERIA

WITH A SPECIAL LOOK AT

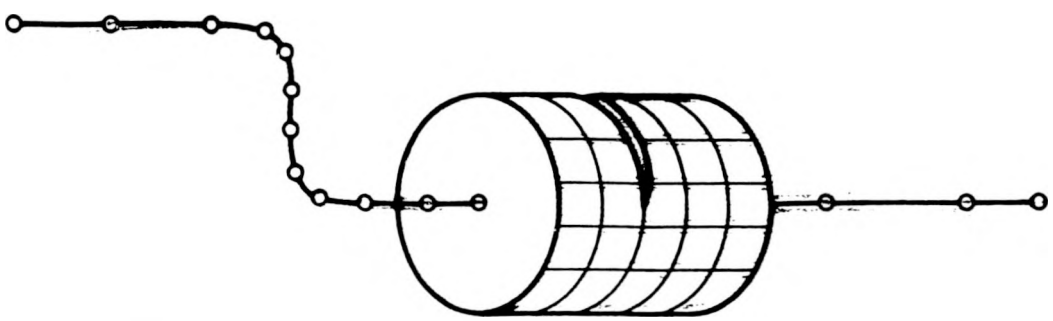
STRESS CLASSIFICATION

WELDS WITH STRESS-STRAIN CURVE

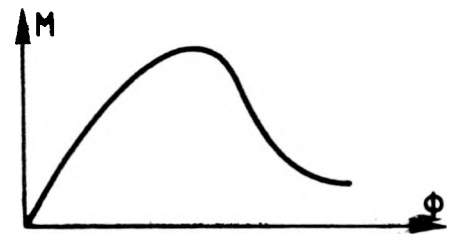
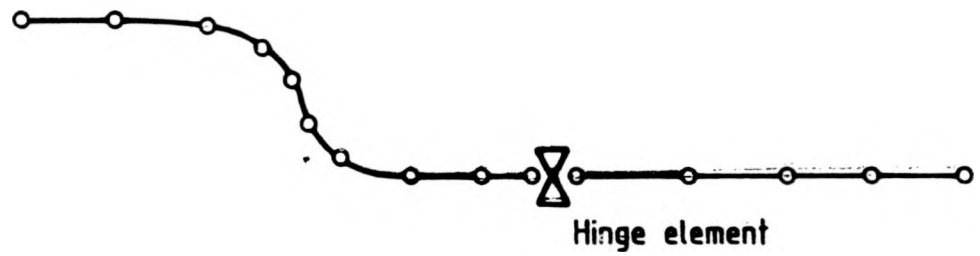
DIFFERENT BASE METAL S.S. CURVE

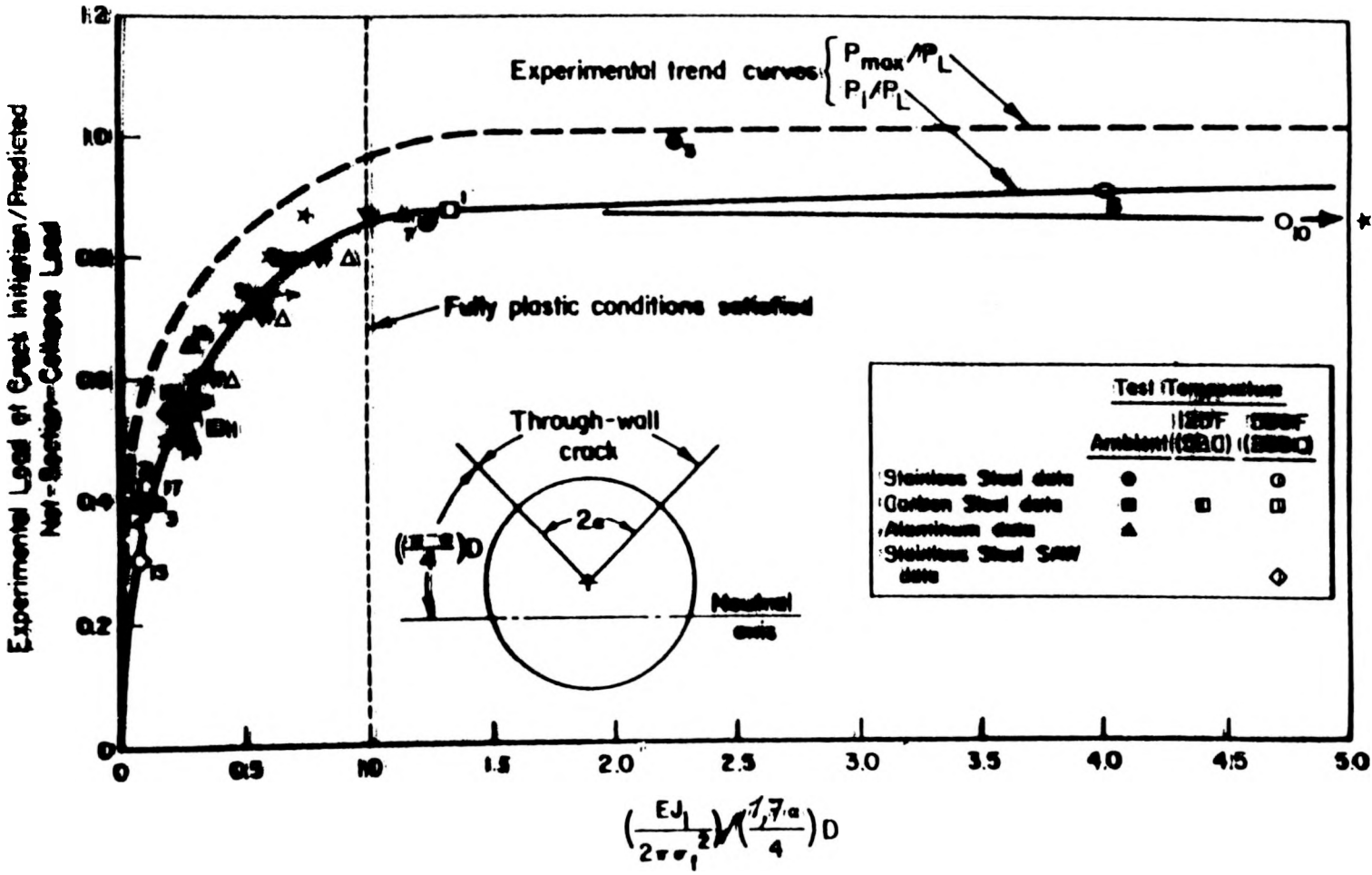
ANALYSES

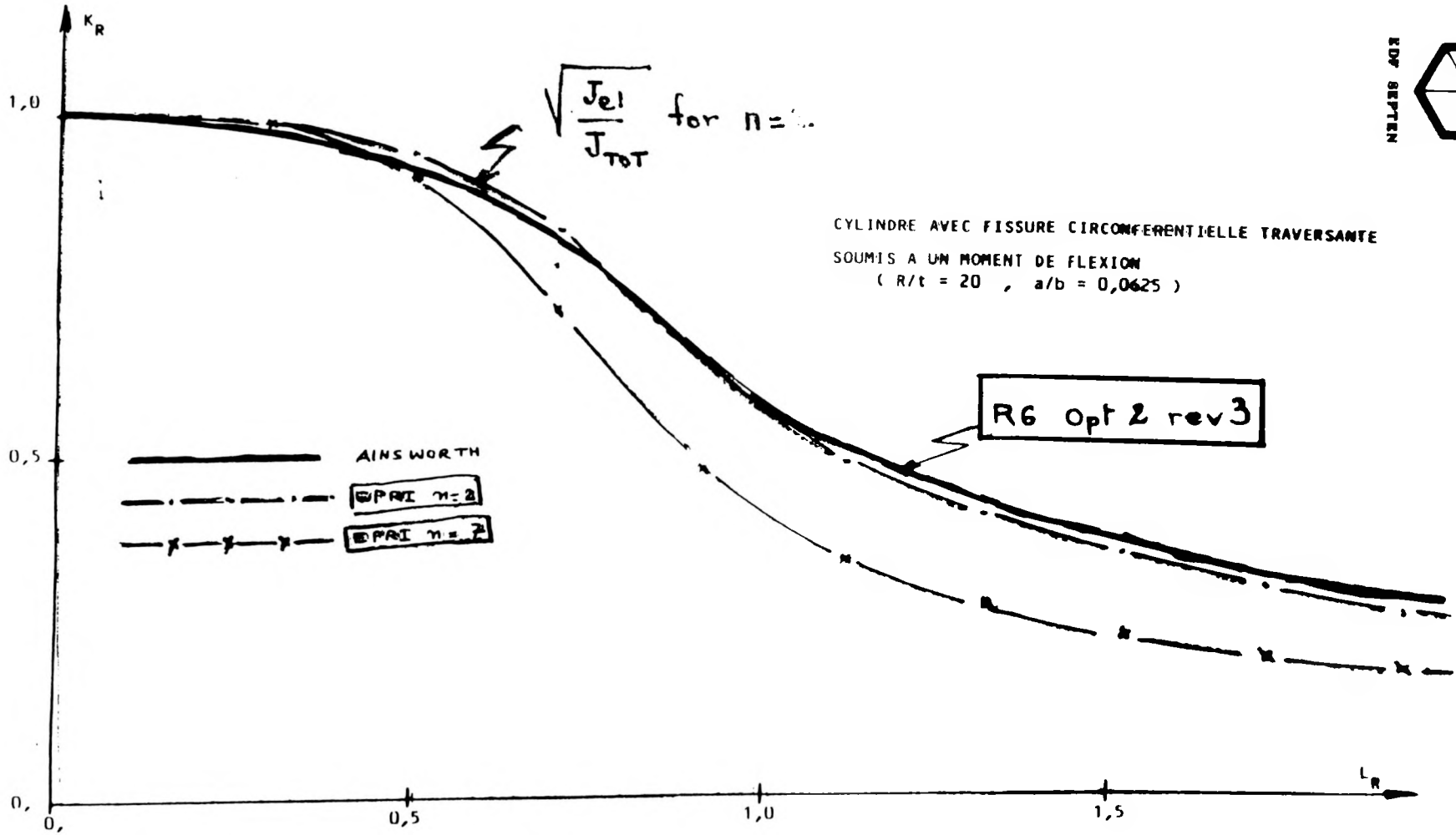
*** 3 - D ANALYSES**



*** 1 - D ANALYSES : Development of an hinge element**







F.E.M. / EXPERIMENTAL RESULTS

- THIN SHELL / SMALL DISPLACEMENT / V.C.E.

TEST DESCRIPTIONS

	Carbon Steel CS	Stainless Steel SS
<u>external diameter</u>	<u>6"</u> (168.9 mm)	<u>4"</u> (97.05 mm)
<u>thickness</u>	10.9 mm	8.4 mm
<u>pipe length</u>	2 300 mm	2 120 mm
<u>test temperature</u>	300°	20°
<u>crack angle</u>	120°	120°
L	1 100 mm	700 mm
Z	1 900 mm	2 000 mm

MATERIAL PROPERTIES

	CS 300°C	SS 20°C
E (MPa)	190 000	139 500
<u>S_y</u> (MPa) $R_{0.2}$	<u>330</u>	<u>217</u>
S _u (MPa)	525	514
<u>J_{IC}</u> (KJ/m ²)	<u>180</u>	<u>> 1 000</u>

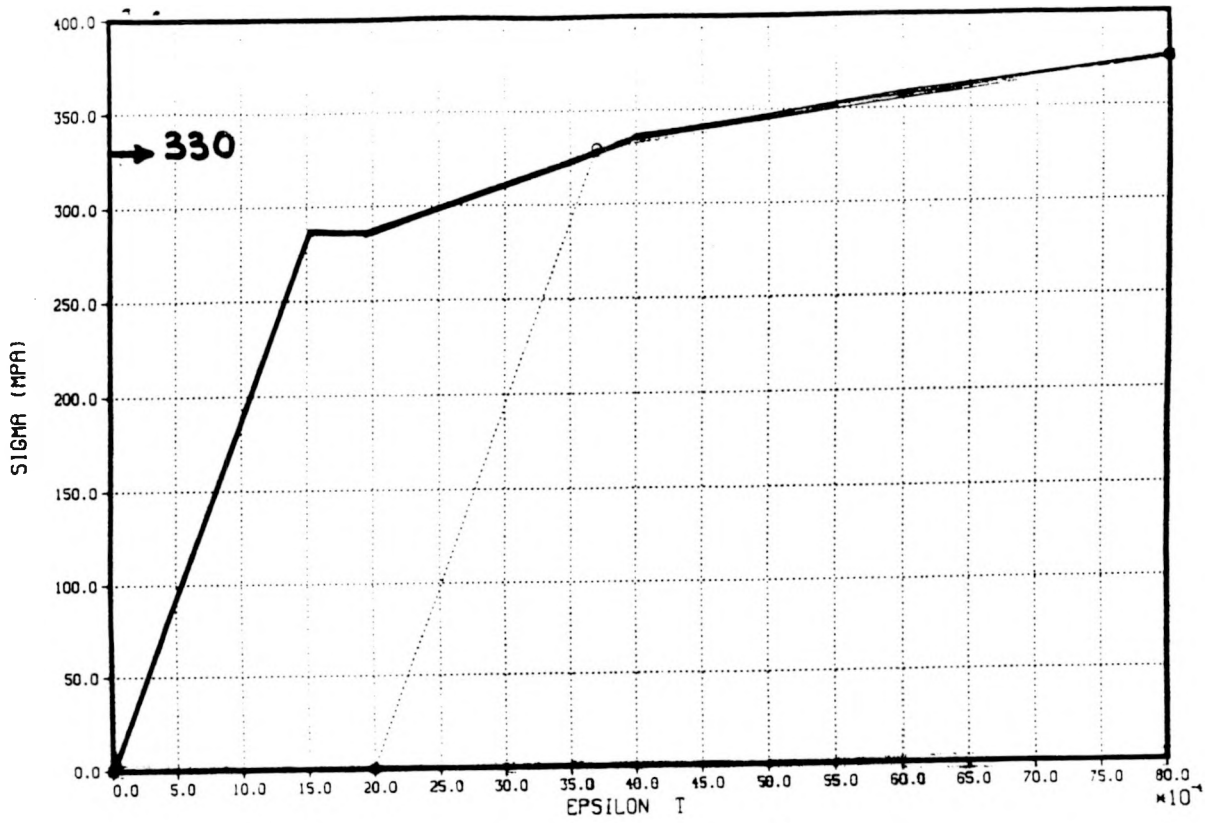
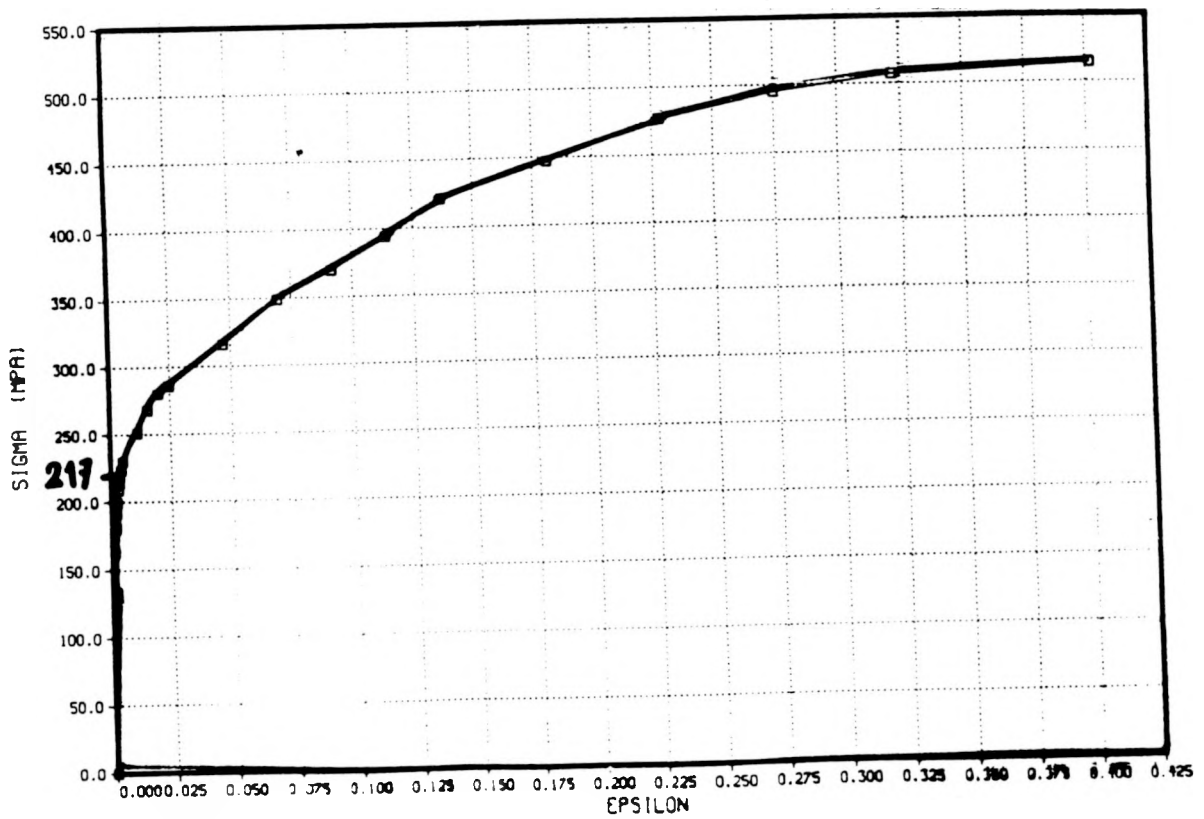


Fig. 3 - Stress strain curves

3.a - C.S.

3.b - S.S.



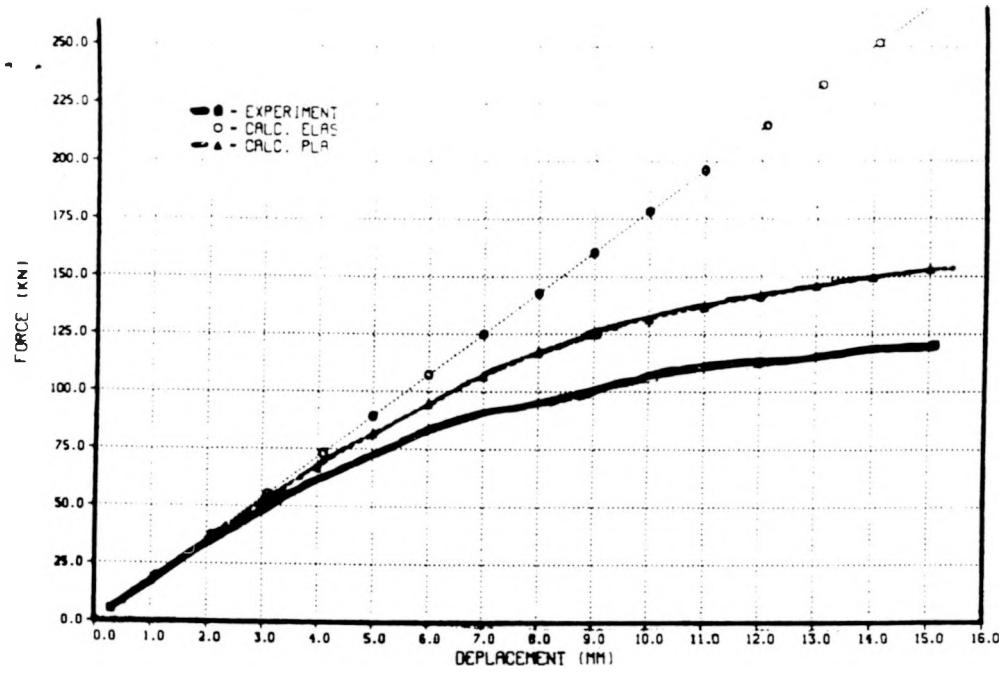
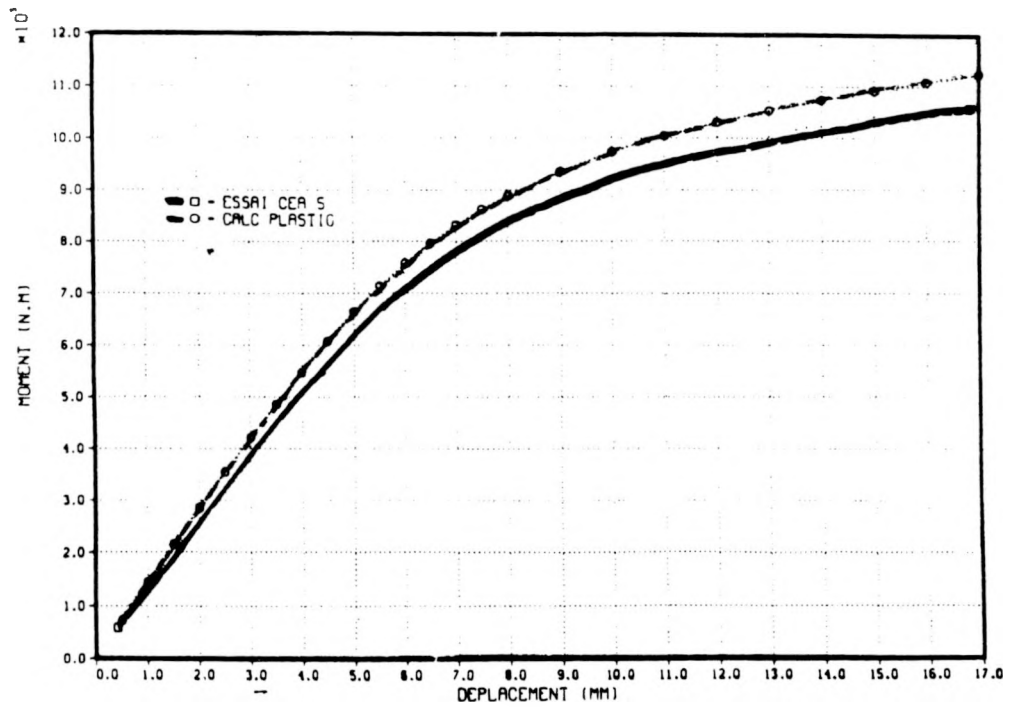


Fig. 4 - Load displacement curves

4.a - C.S.
 4.b - S.S.



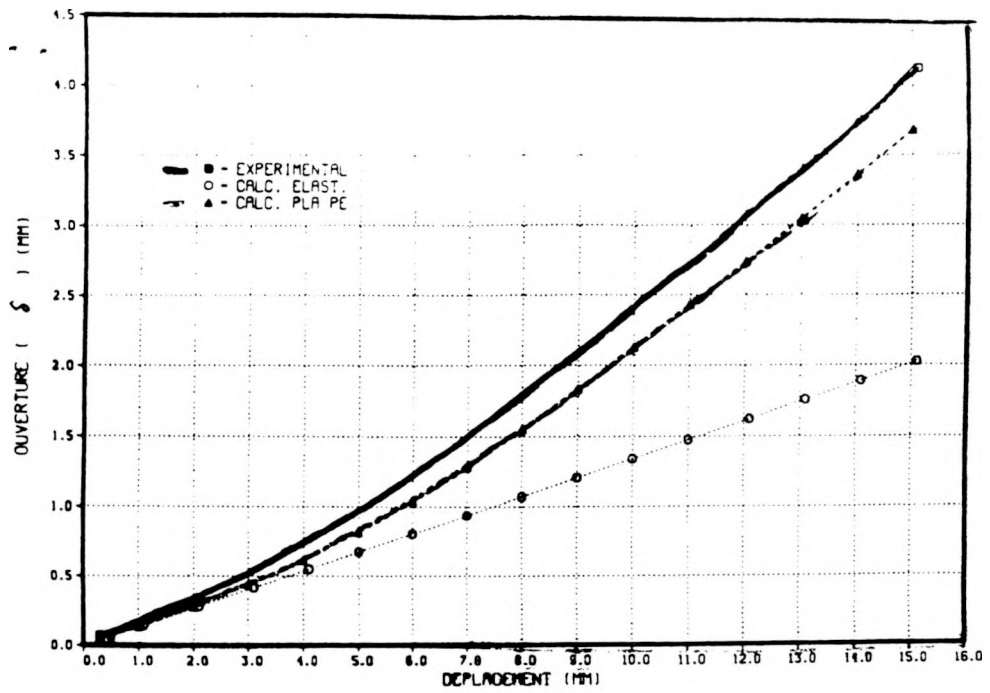
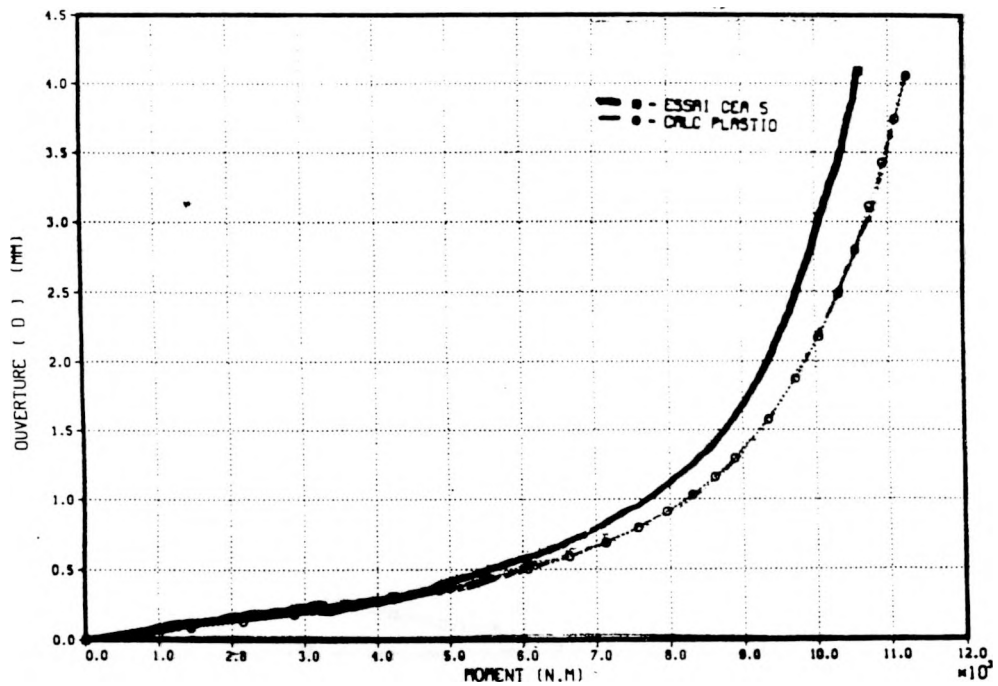
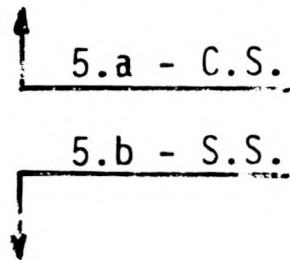


Fig. 5 - **CMOD**



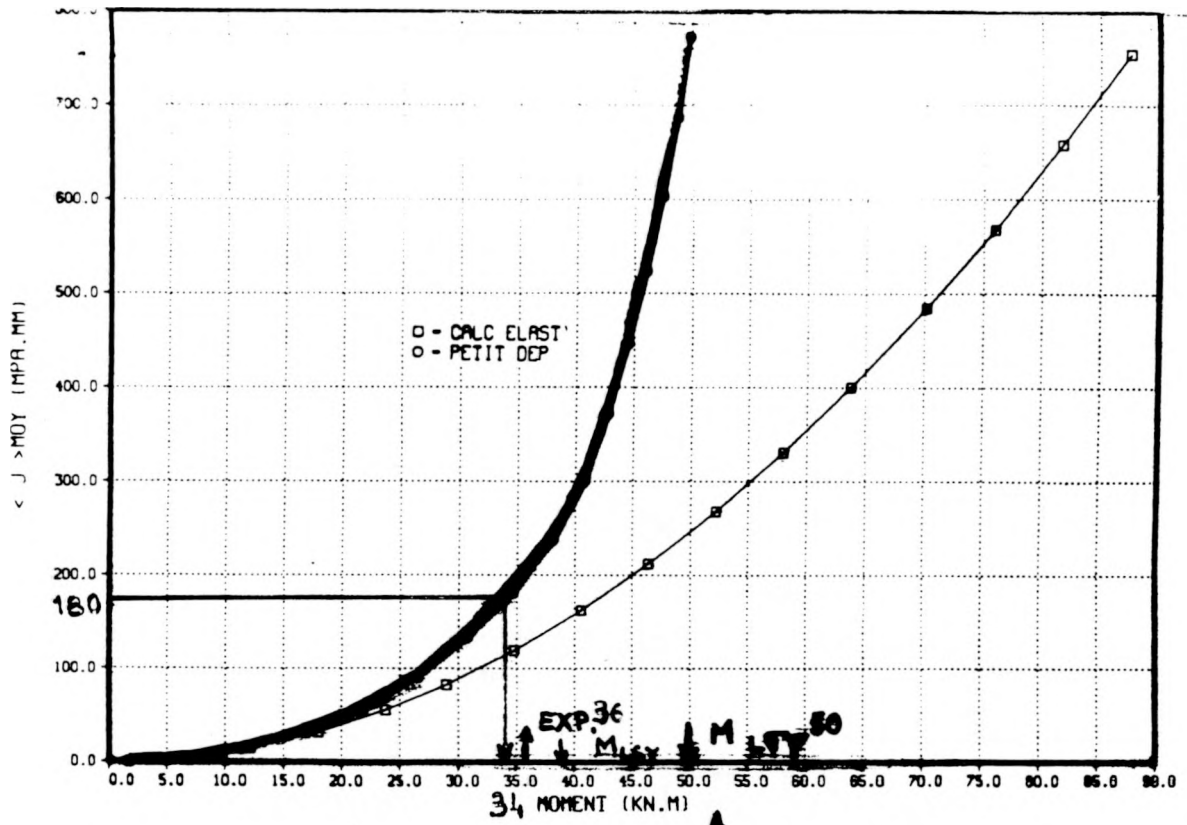
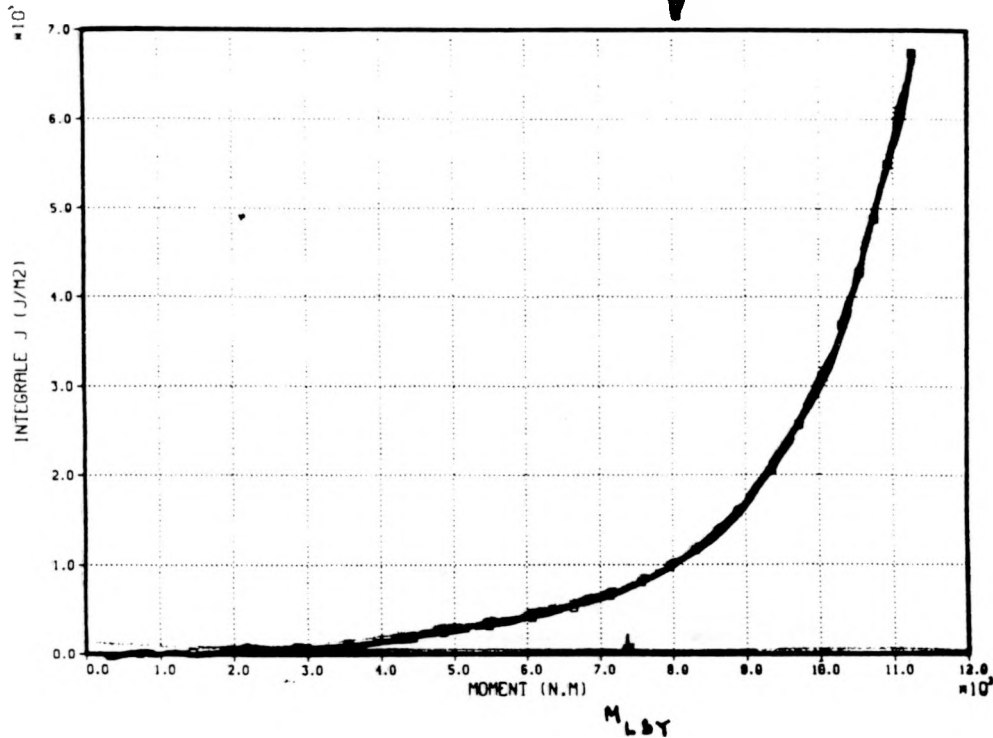


Fig. 7 - J versus moment

7.a - C.S.

7.b - S.S.



DETAILED COMPARISONS

LOAD DISPLACEMENT

CS : MORE THAN 20%
SS : MORE THAN 6% ON LOAD

CRACK MOUTH OPENING DISPLACEMENT

UNDERESTIMATION LESS THAN 20%
CONSERVATIVE FOR L.B.B.

OVALIZATION

SMALL DISPLACEMENT THEORY
V LESS THAN 1%

J VERSUS MOMENT

CS : $M_I = 36.6 \text{ kNm}$

$M_{LSY} = 38.9 \text{ kNm}$

WITH CT J_{IC} : $M_I = 34 \text{ kNm}$

SS : NO CRACK INITIATION BEFORE MAXIMUM MOMENT

$M_{LSY} = 7.4 \text{ kNm}$

Exp $M_{MAX} = 11.8 \text{ kNm}$

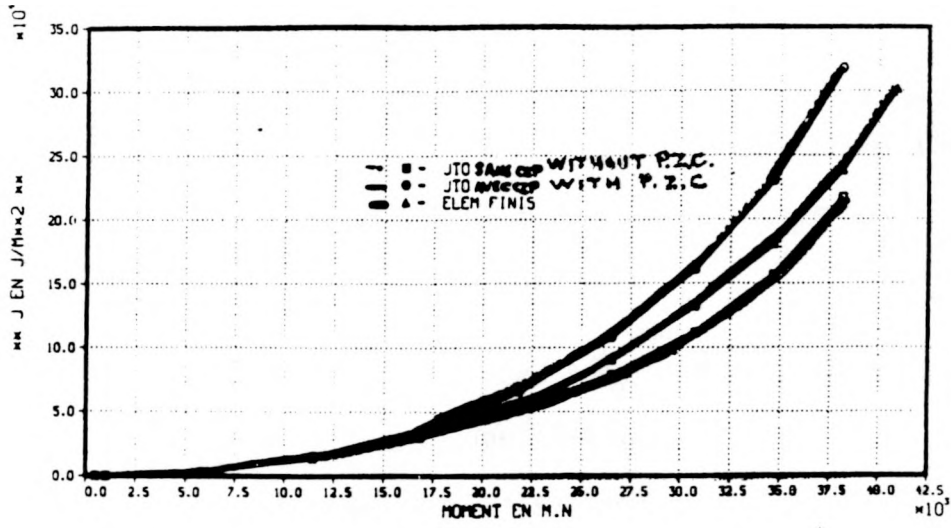
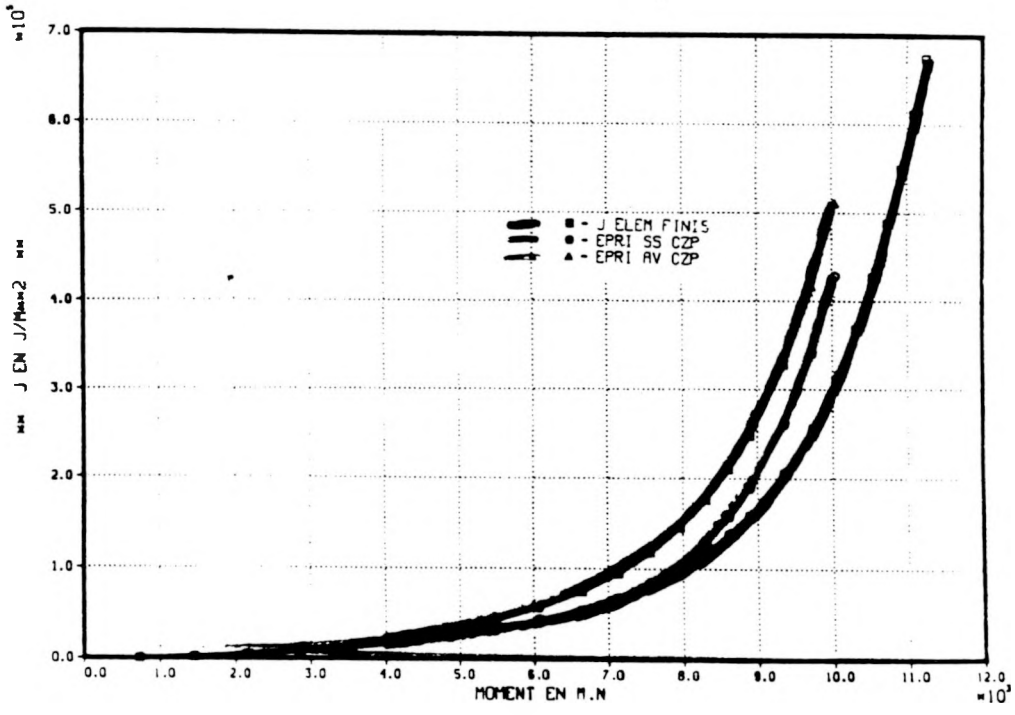


Fig. 9 - EPRI J versus moment

9.a - C.S.

9.b - S.S.



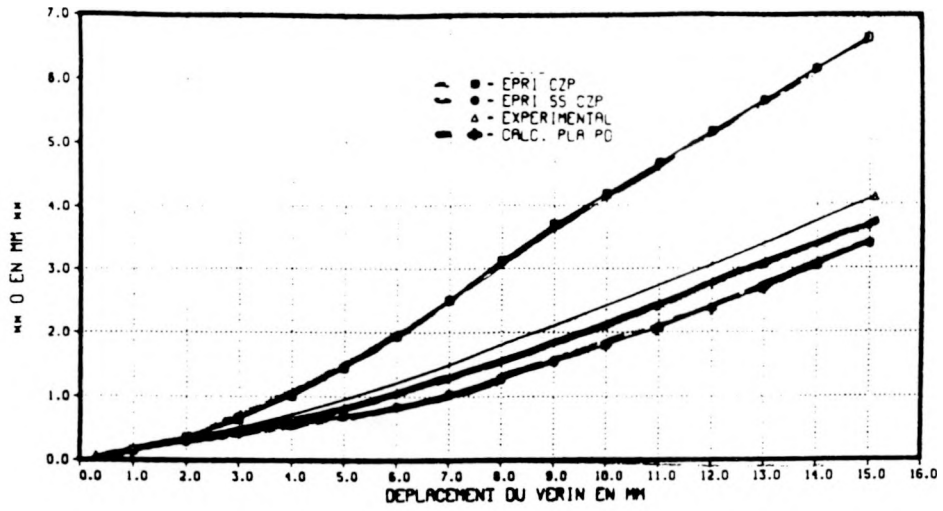
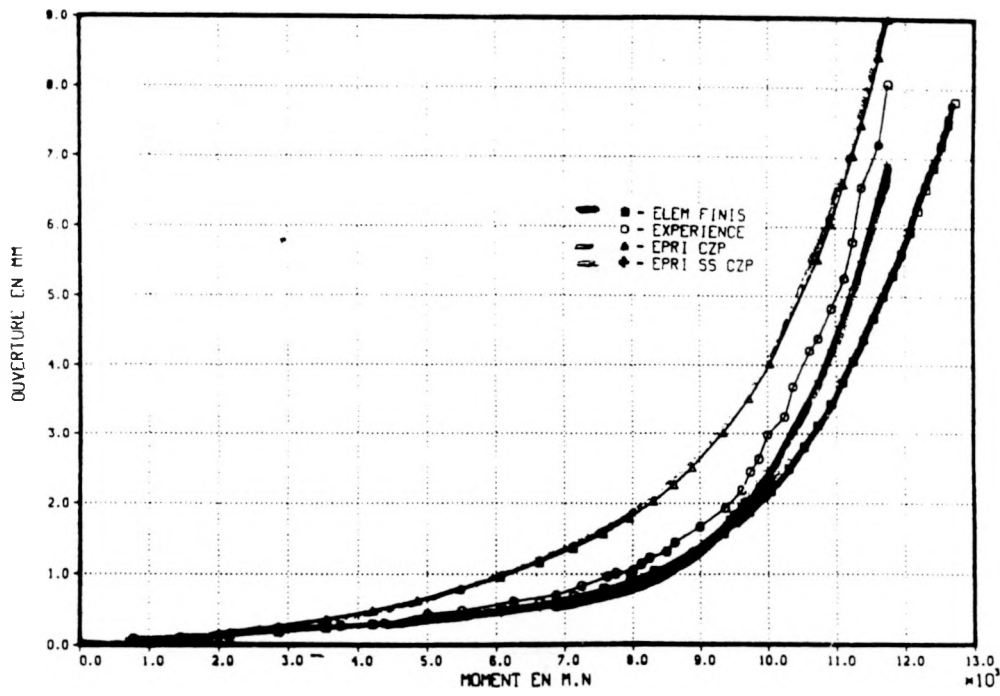
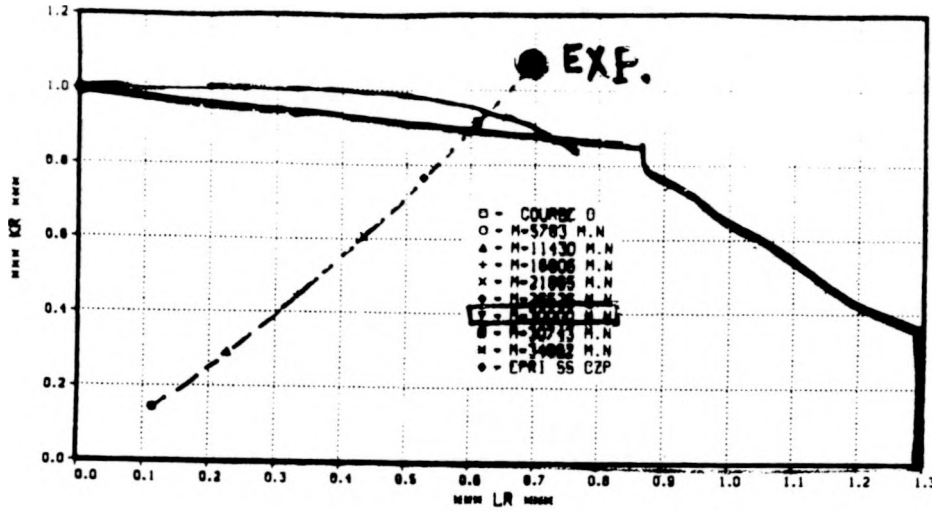


Fig. 8 - EPRI O versus moment

8.a - C.S.
8.b - S.S.

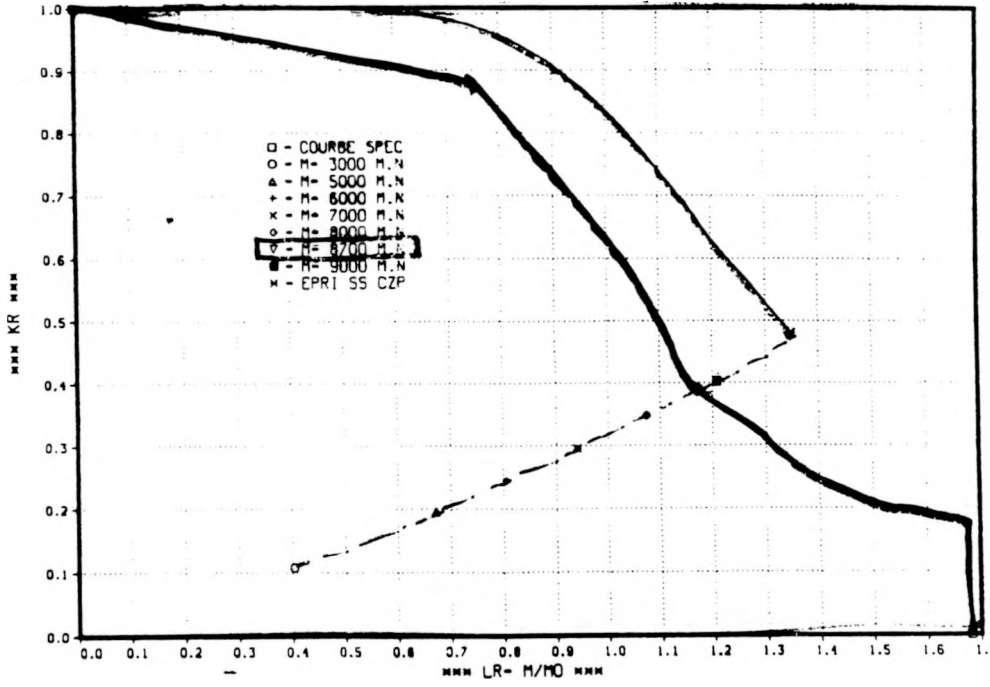
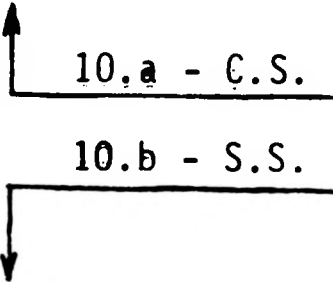


$$K_R = \frac{K}{K_{sc}}$$



$$L_R = M / M_{LST}$$

Fig. 10 - R6 diagram



ENGINEERING METHODS

GE - EPRI

R.O. PARAMETERS

CS : N = 7.17 $\alpha = 1.12$

SS : N = 8.72 $\alpha = 1.31$

M_{LSY}

SS : $M_L = 7.44$ kN.m

cS : $M_L = 38.9$ kN.m

J_{PL} SMALL PART OF J_{TOT}

NO PLASTIC ZONE CORRECTION NEEDED FOR J OR CMOD APPROACH

P.Z.C. IS CONSERVATIVE FOR J; UNCONSERVATIVE FOR CMOD

R6 APPROACH

OPTION 2 - REV. 3 - CEBG.R6

IN THIS CASE, MORE CONSERVATIVE THAN GE-EPRI APPROACH

SEISMIC TESTS

AIM OF STUDY

INFLUENCE OF FREQUENCY (EXCITATION/PIPING)

INFLUENCE OF DUCTILITY ON DYNAMIC CRACK LOADING

INFLUENCE OF CRACK POSITION

INFLUENCE OF CRACK ON MODE COUPLING

TESTS ON

STRAIGHT PIPES

ELBOWS

SAMPLE LINE

STRAIGHT PIPES

GEOMETRY

STATIC TESTS

$$\theta_i = 90 \text{ mm}$$

$$e = 8.36 \text{ mm}$$

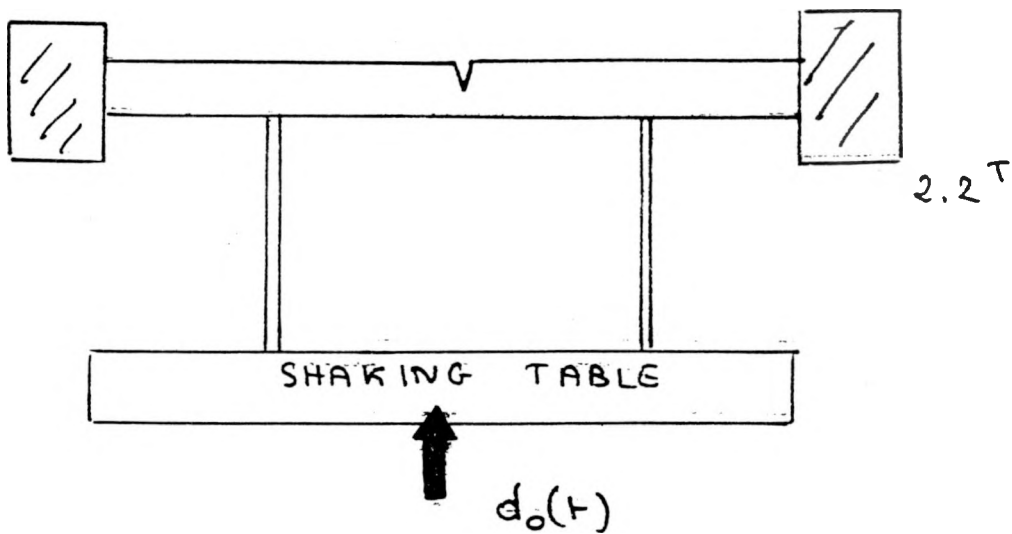
STAINLESS

NO PRESSURE

NO TEMPERATURE

(REFERENCE STATIC TESTS EXIST)

TESTS ON VERTICAL SHAKING TABLE



TESTS

WITHOUT CRACK

3 TESTS WITH 120° CRACK

SINUSOIDAL MOTION : f_0 12

f_0

3 f_0 12

LEVEL VERSUS INSTABILITY IN 15 CYCLES

2 CONTINGENCY TESTS (f'_0 , GRAVITY, ...)

ELBOWS

GEOMETRY

STRAIGHT PIPES

$$\theta_i = 90 \text{ mm}$$

$$e = 8.36 \text{ mm}$$

90° ELBOW

(REFERENCE STATIC TESTS EXIST)

TESTS (THREE TYPES)

WITHOUT CRACK

CRACK INTRADO

CRACK EXTRADO

(CONFIRMATION OF STRAIGHT PIPE RESULTS)

SIMPLE LINE

GEOMETRY AS IN PREVIOUS TESTS

TESTS (THREE TYPES) ON SHAKING TABLE

1 TEST WITHOUT CRACK

2 TESTS WITH CRACK

2 DIFFERENT INPUTS (FREQUENCY CONTENT)

SAME MOTION FOR ALL SUPPORTS

OBJECTIVE TWO MODES ARE IMPORTANT FOR CRACK LOADING

FRENCH R & D PROGRAM

QUASI STATIC TESTS	1988 - 1989
PROTOTYPES	
ANALYTICAL	
DYNAMIC TESTS	1989 - 1990
DEVELOPMENT AND VALIDATION OF ANALYTICAL METHODS	1988 - 1989
F.E.M.	
1-D CRACKED ELEMENT	
ENGINEERING METHODS	
TEST ON SINGULARITIES (NOZZLES, ELBOWS, ...)	1990 - 1991
RECOMMENDATIONS	1989 - 1990

FUTURE WORK

- 1. COMPLETE THE STATIC TESTS**
- 2. CRACK GROWTH TEARING ANALYSIS**
F.E.M.
ENGINEERING
- 3. ANALYSIS OF FRACTURE MODE OF GS**
- 4. ENGINEERING METHODS**
CONSEQUENCES OF WELDS
STRESS CLASSIFICATIONS
COMBINED LOADS
- 5. DYNAMIC ANALYSIS OF SEISMIC TESTS**

INTERNATIONAL PROGRAMS

DEGRADED PIPING PROGRAM (DP²III)

MORE THAN 70 TESTS ON PIPING

INTERNATIONAL PIPING INTEGRITY RESEARCH GROUP (IPIRG)

SEISMIC TESTS ON CRACKED PIPES

LOAD CONTROL

DISPLACEMENT CONTROL

SIMPLE 5 ELBOWS TESTS

EDF - NOVATOME - INTERATOM TESTS

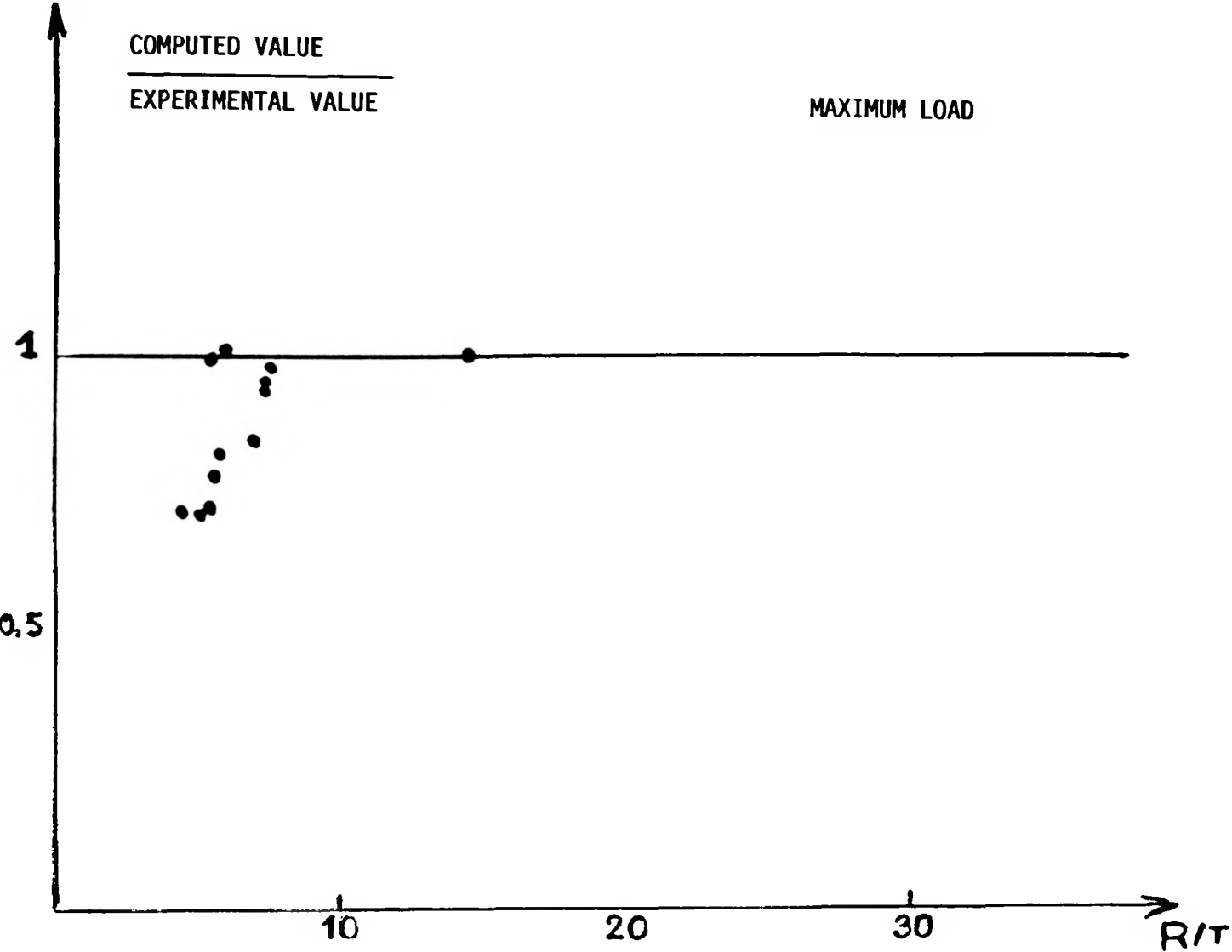
8 BENDING TESTS ON FAST BREEDER PIPING ($R/t \approx 37$)

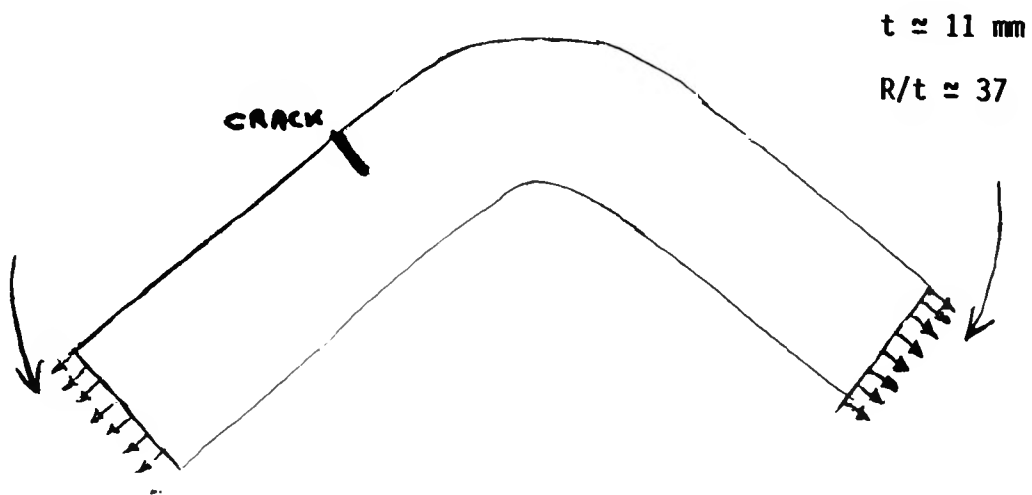
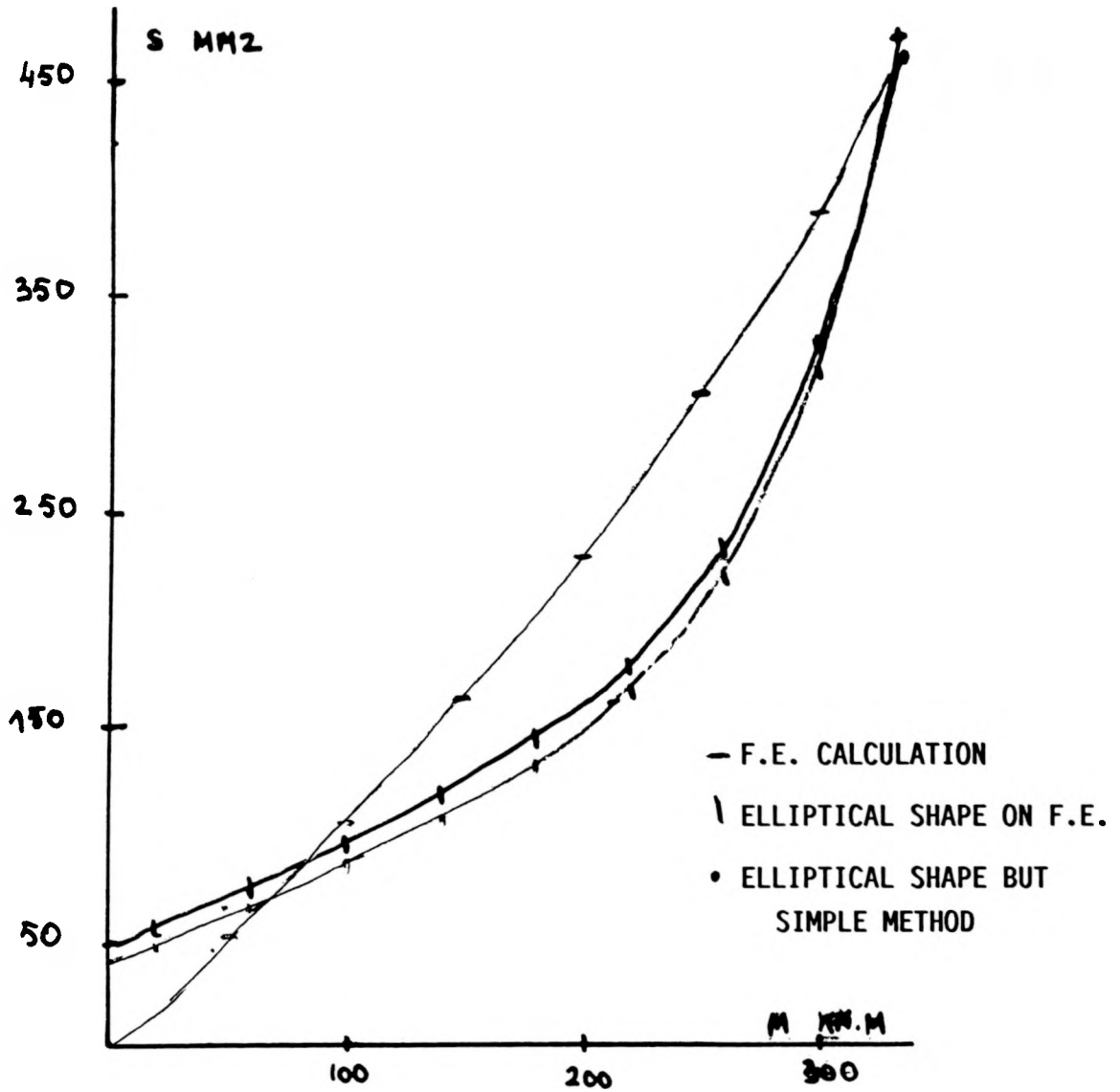


COMPUTED VALUE

EXPERIMENTAL VALUE

MAXIMUM LOAD





CONCLUSIONS

PRACTICAL APPLICATIONS ARE PRESENTLY LIMITED

EDF, FRAMATOME, AND CEA ARE DETERMINED TO DEFINE A COMPLETELY VALIDATED METHODOLOGY

R & D IN NATIONAL OR INTERNATIONAL COOPERATIVE AGREEMENTS WILL BE ABLE TO YIELD IMPORTANT RECOMMENDATIONS FOR FRACTURE MECHANICS CONSIDERATIONS IN THE BEGINNING OF 1990

EDF AND FRAMATOME ARE CONFIDENT IN LBB DEMONSTRATION IN PWR IN FRANCE

MANY PARTS OF THIS COOPERATIVE WORK WILL BE PRESENTED IN DIFFERENT CONFERENCES IN THE NEXT FEW MONTHS (SEOUL, SMIRT, PVP, TORONTO, ...)

**CRACK INITIATION AND EXPERIMENTAL DETERMINATION
OF J IN BENDING FOR ELBOWS AND PIPES
OF AUSTENITIC STEEL**

P. Jamet, D. D. Moulin, F. Toubol, J. Lebey, and D. Acker

A B S T R A C T

This paper presents tests performed on cracked straight pipes, as well as elbows subjected to closing or opening bending moments. The material used is stainless steel.

The obtained results show that there is a special effect of short cracks with respect to maximum bending moment.

It is also shown experimentally that the usual procedure to determine J with a single cracked straight pipe can be applied to elbows.

CRACK INITIATION AND EXPERIMENTAL
DETERMINATION OF J IN BENDING
FOR ELBOWS AND PIPES OF
AUSTENITIC STEEL

OBJECTIVES OF THE STUDY

- LEAK BEFORE BREAK
- PRESSURIZED WATER REACTOR
- CRACKED PIPING UNDER BENDING LOADS
- STAINLESS STEEL (316L)
 - CONDITIONS OF CRACK INITIATION UNDER MONOTONIC LOADING (THROUGH WALL CRACK)

CONTENTS

- DESCRIPTION OF TESTS

- EFFECT OF CRACK LENGTH
(θ TOTAL CENTER ANGLE)

- LIMIT ANALYSIS

- $M_{\text{initiation}}$

- $J_{\text{initiation}}$

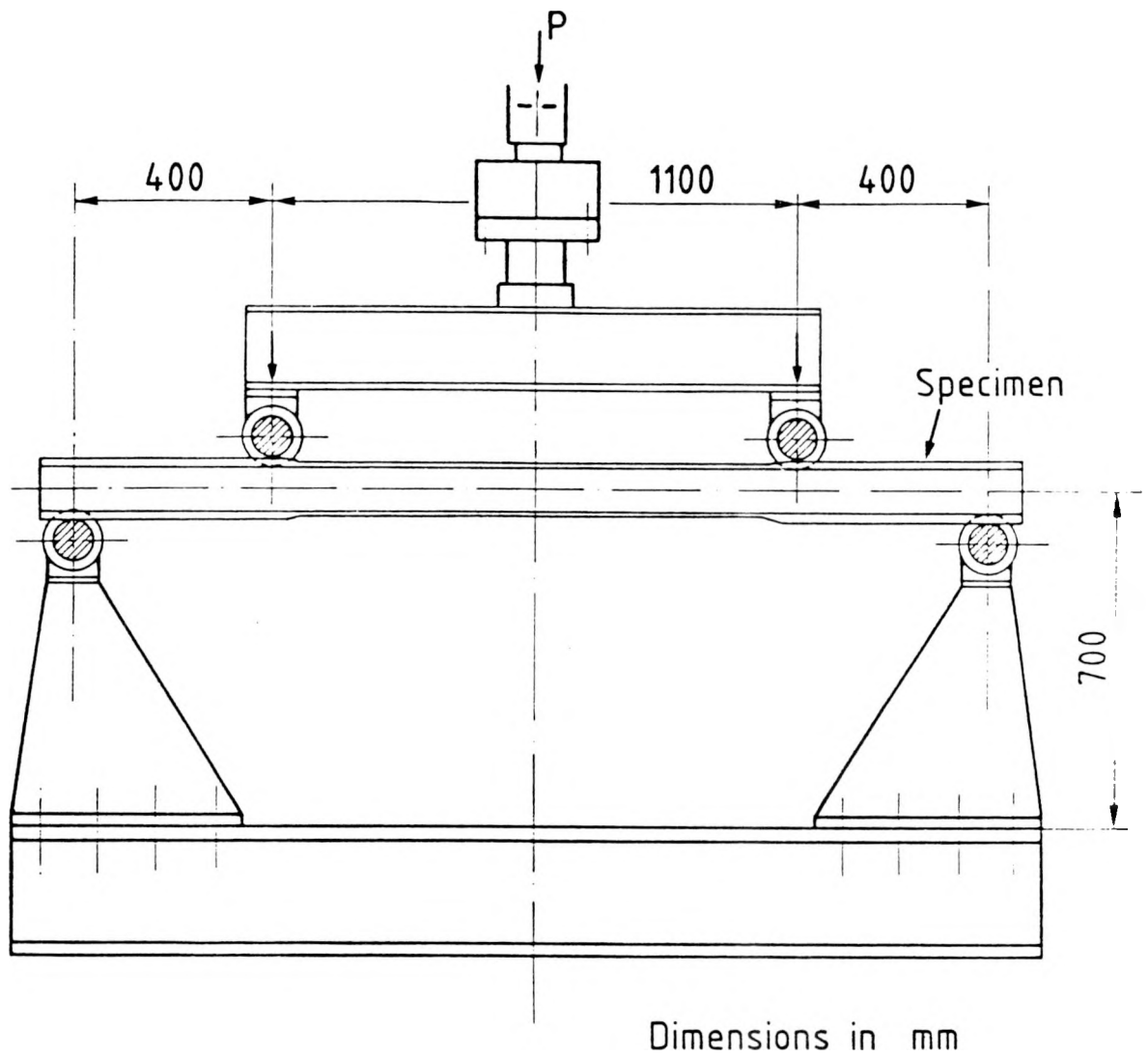
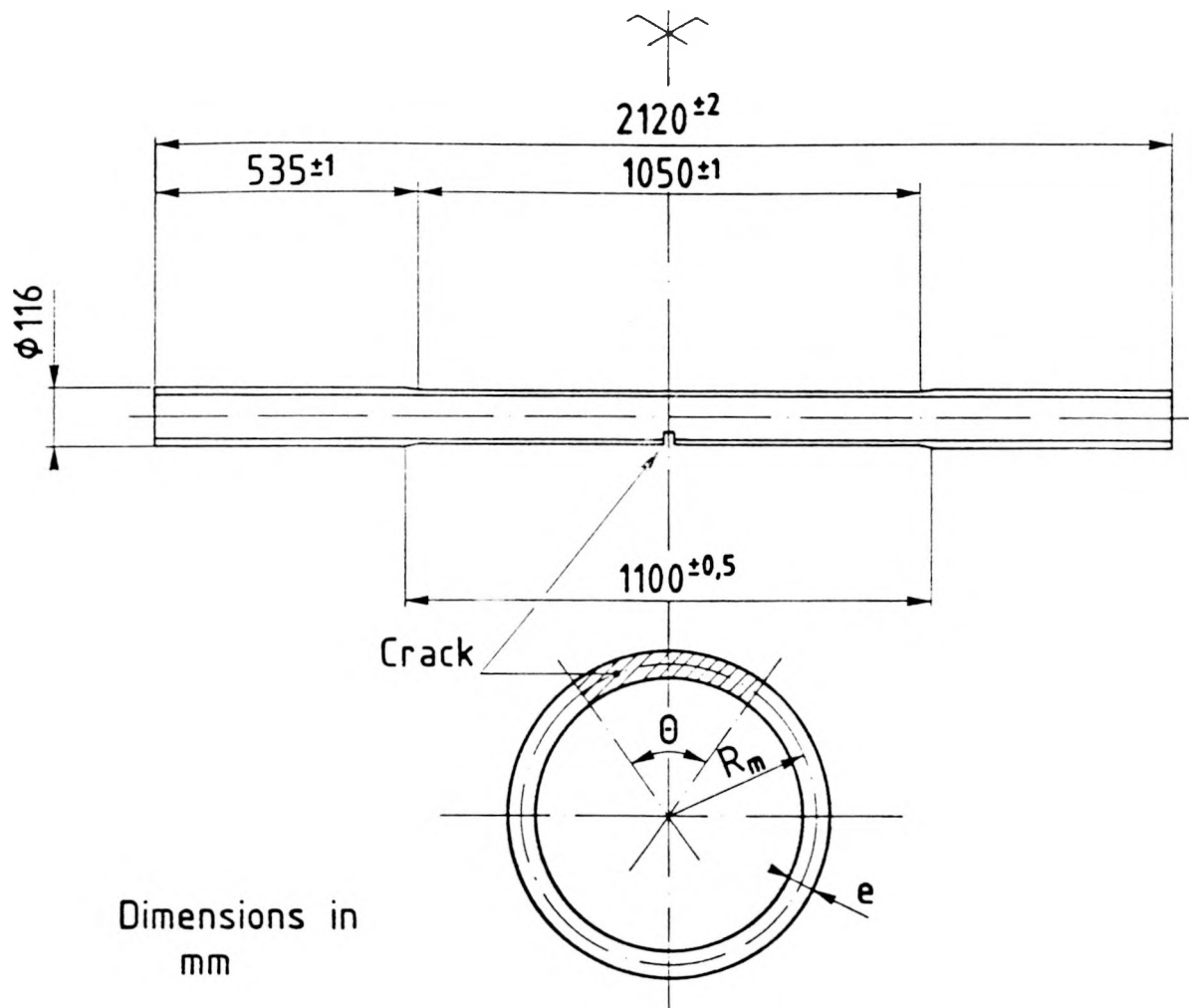


Fig. 1 - EXPERIMENTAL TEST DEVICE FOR STRAIGHT PIPES



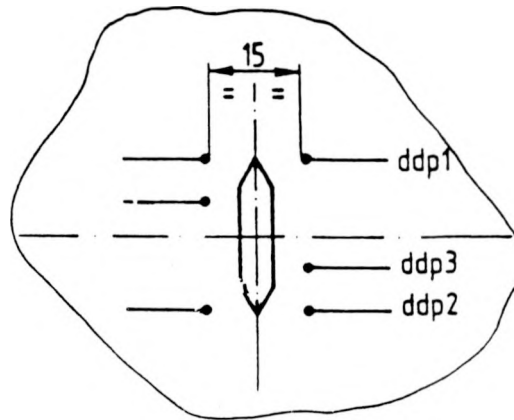
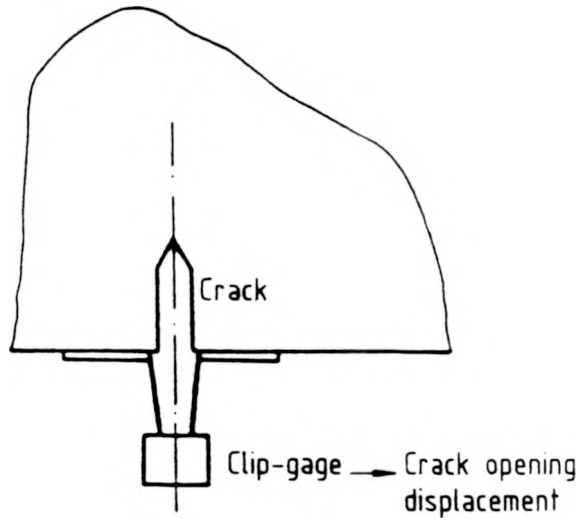
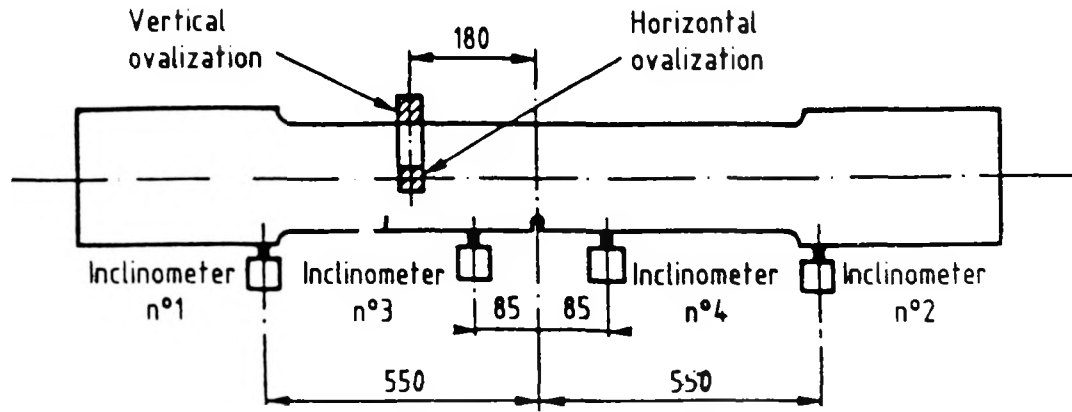
Mean dimensions of cracked section in tubes.

$$R_m = 48,5$$

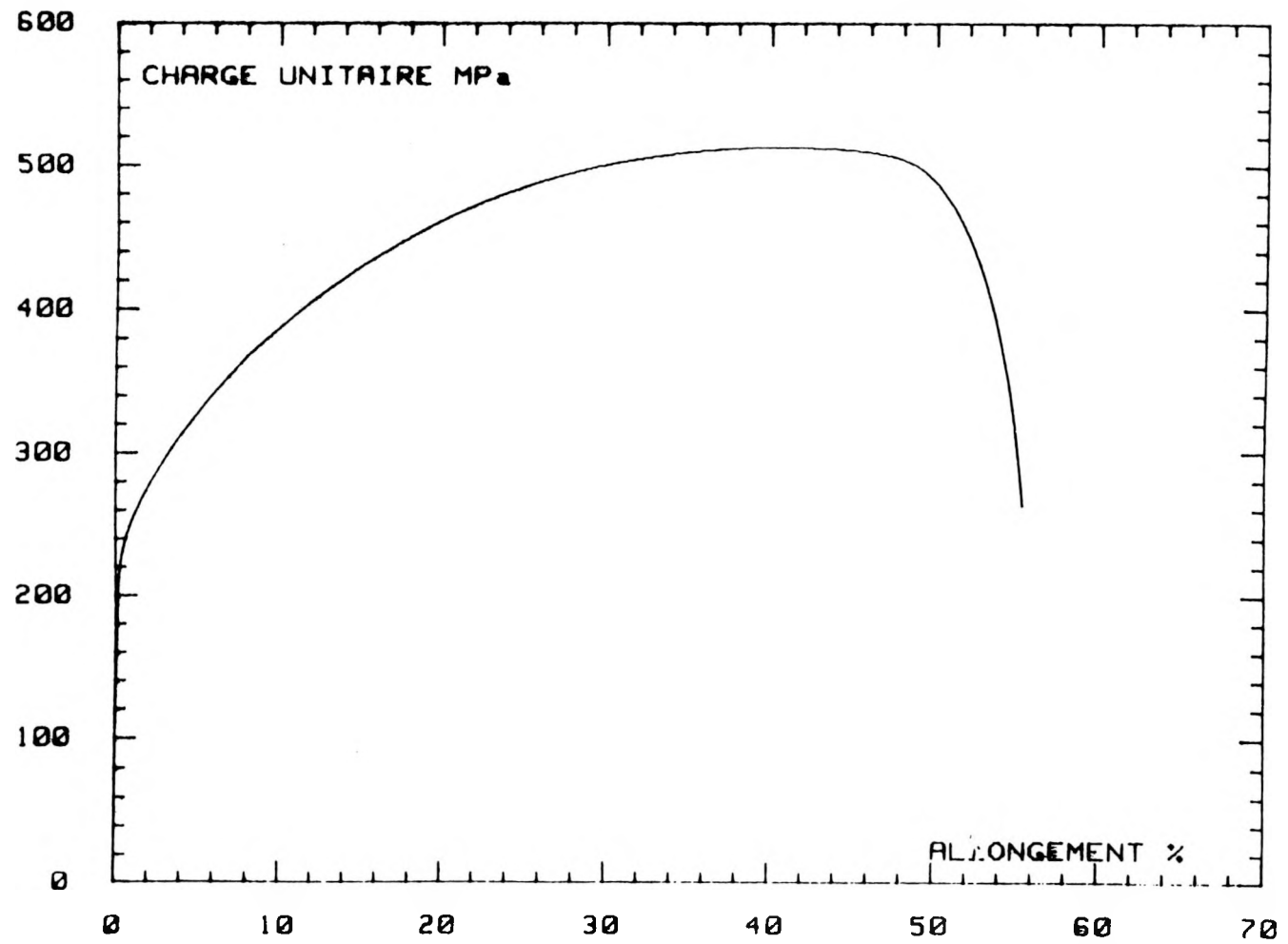
$$e = 8,36$$

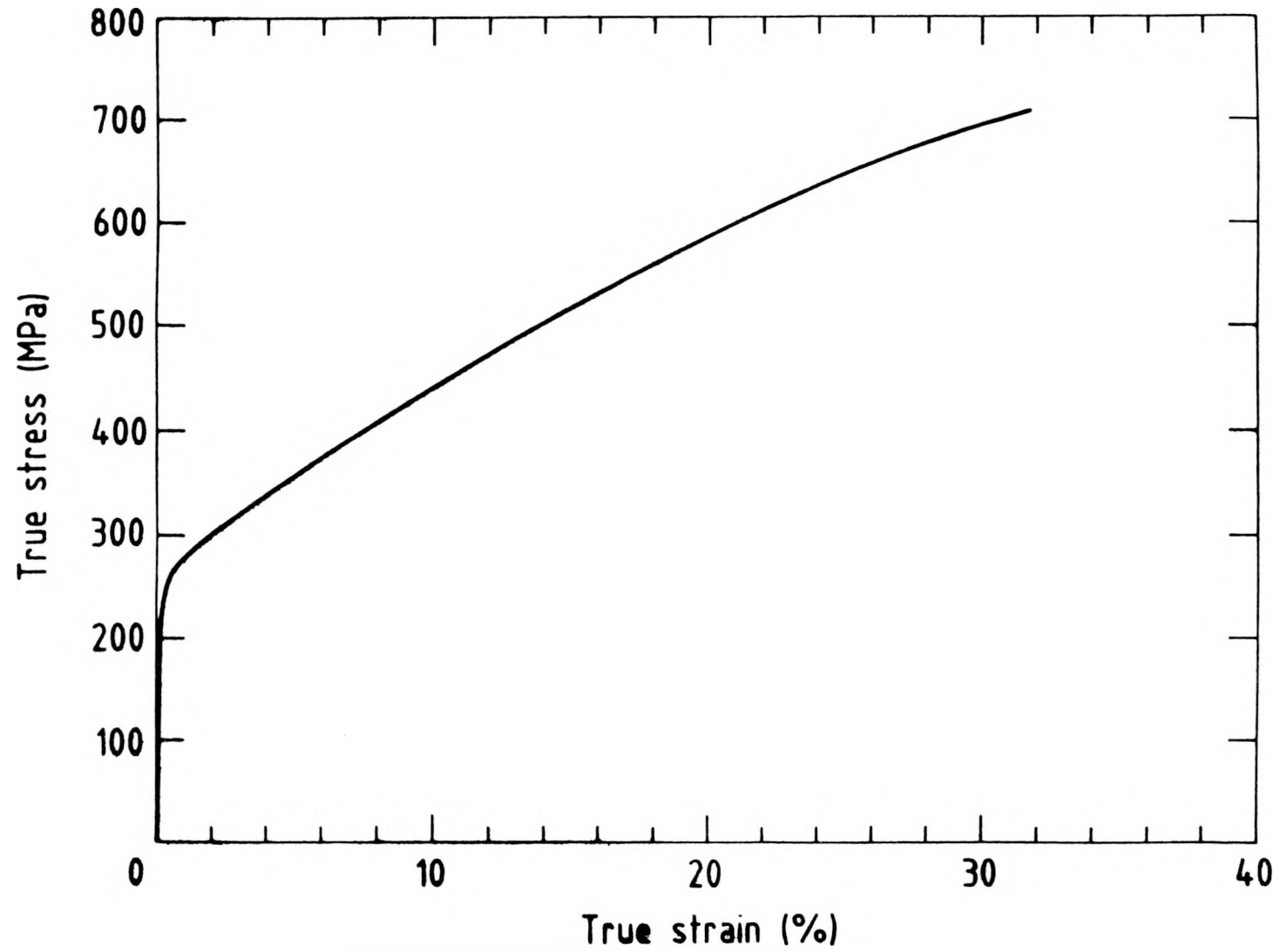
Fig. 2 - SPECIMEN DIMENSIONS

INSTRUMENTATION OF TUBES



EPROUVETTE PRELEVEE DANS LE TUBE N° 02



RATIONAL TENSILE CURVE (TUBE $\theta = 15$ DEGREES)

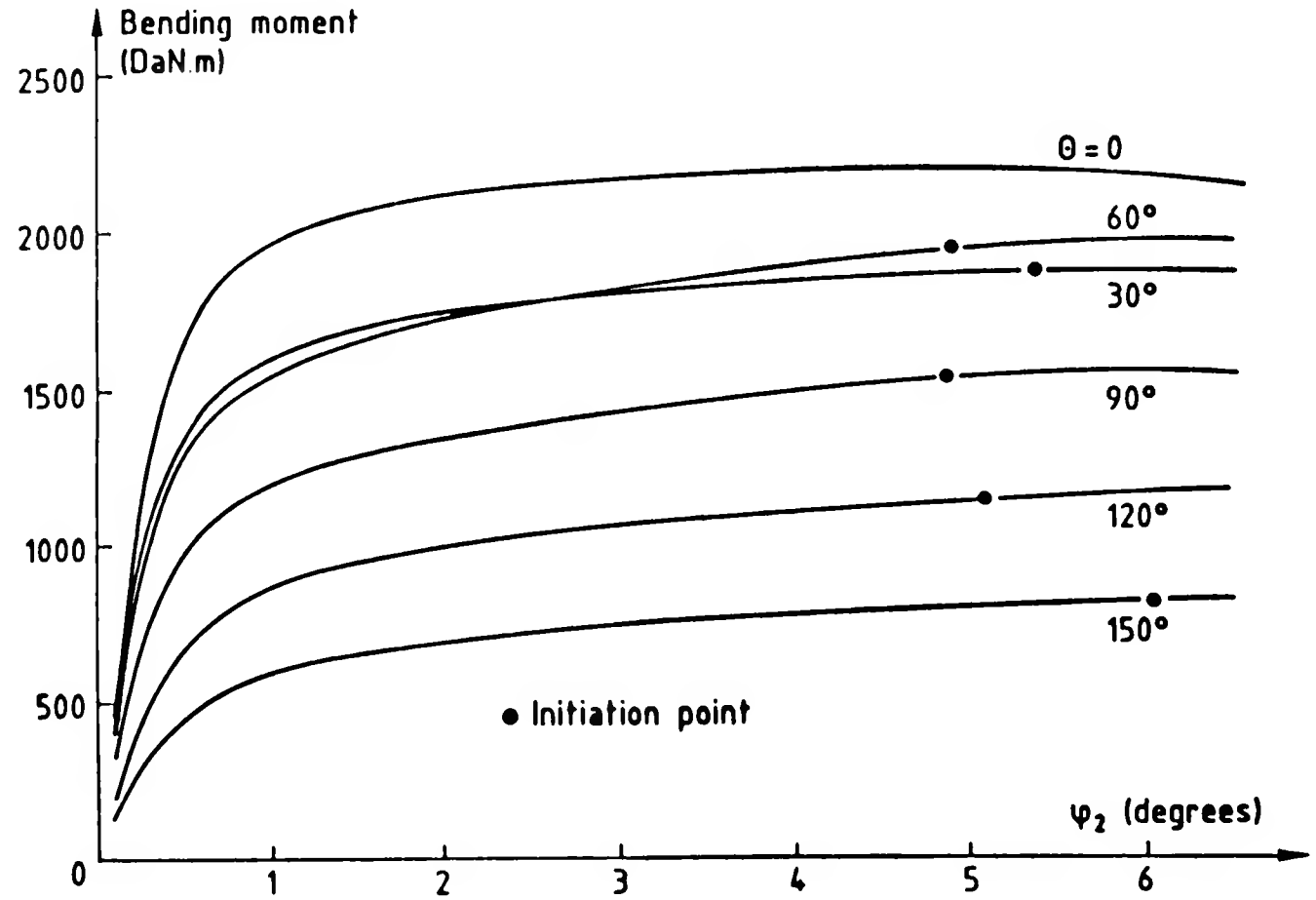


Fig. 4 - BENDING MOMENT-ROTATION ψ_2 CURVES OF TUBES

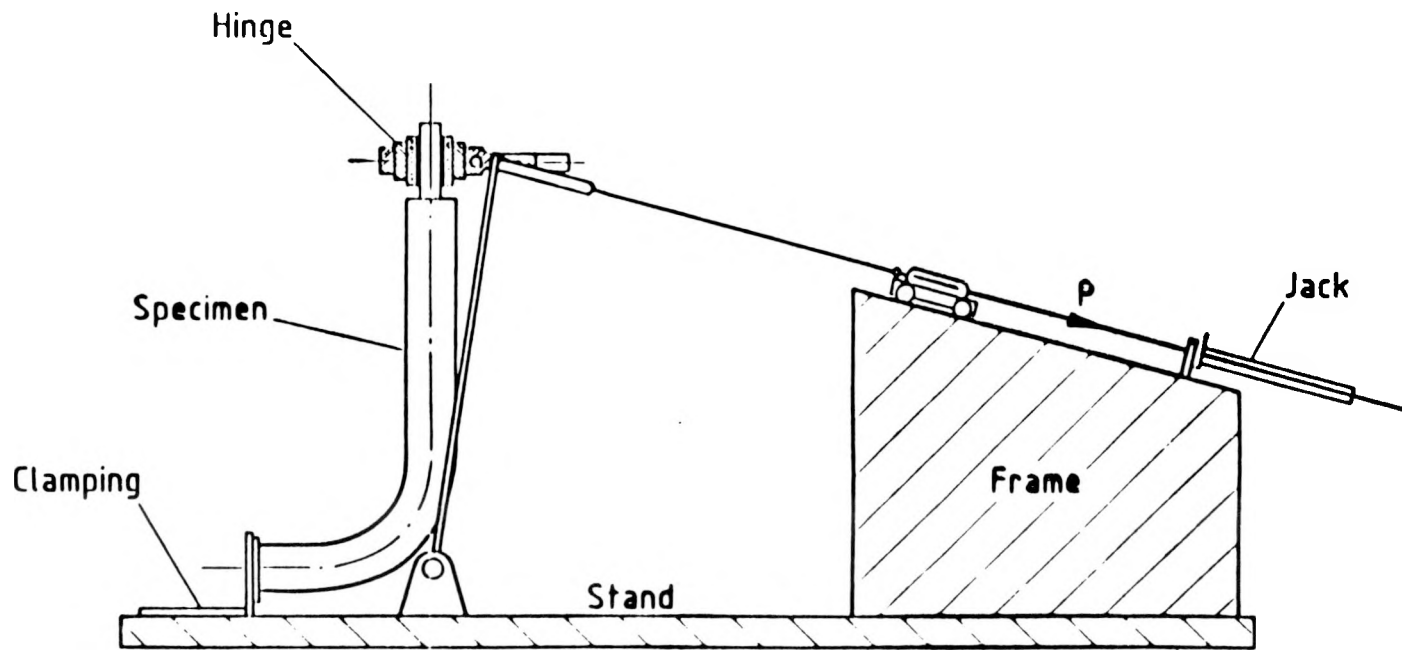
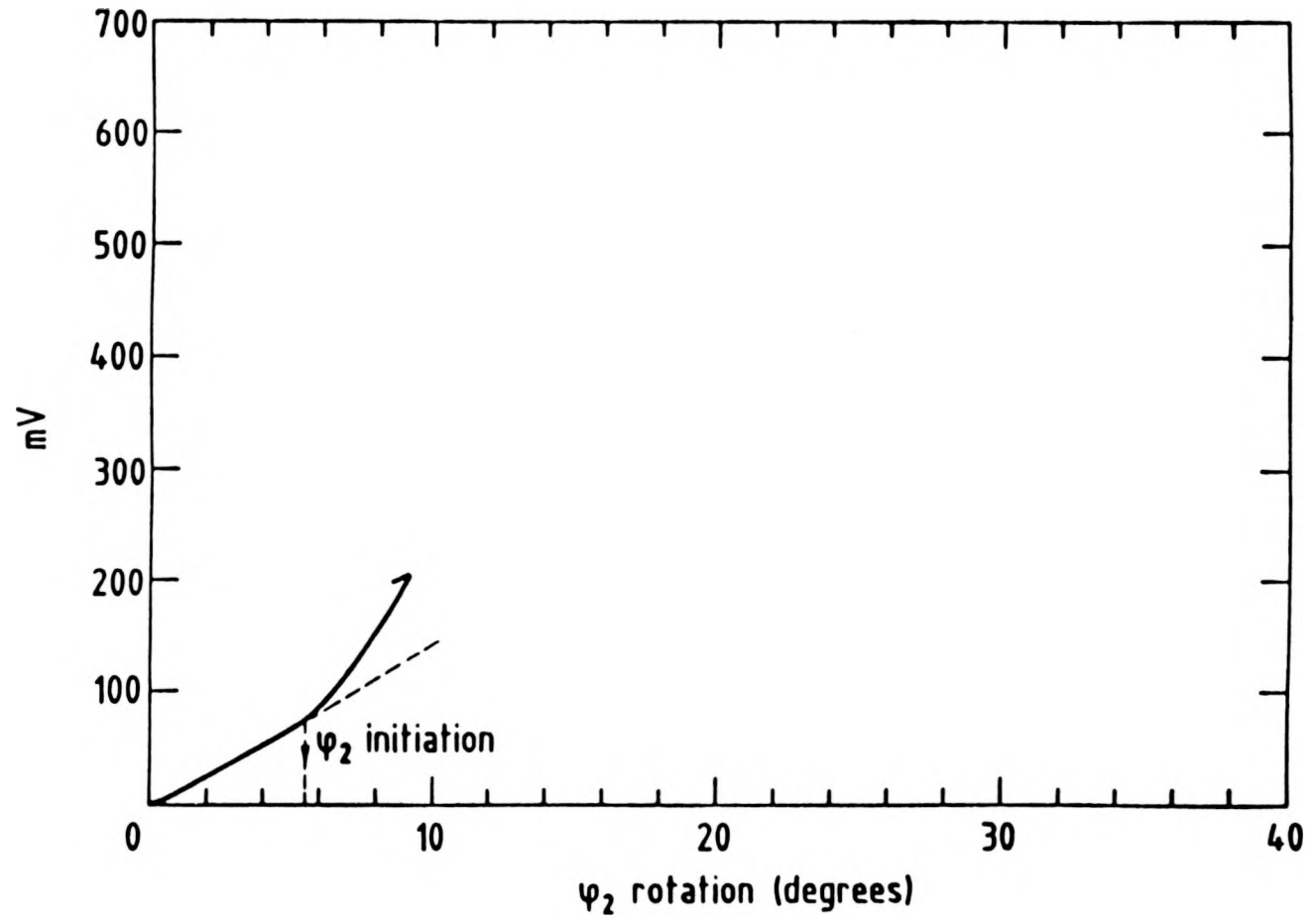


Fig. 5 - EXPERIMENTAL TEST DEVICE FOR ELBOWS BENDING



DETECTION OF CRACK INITIATION ON E.P.D./ROTATION CURVE

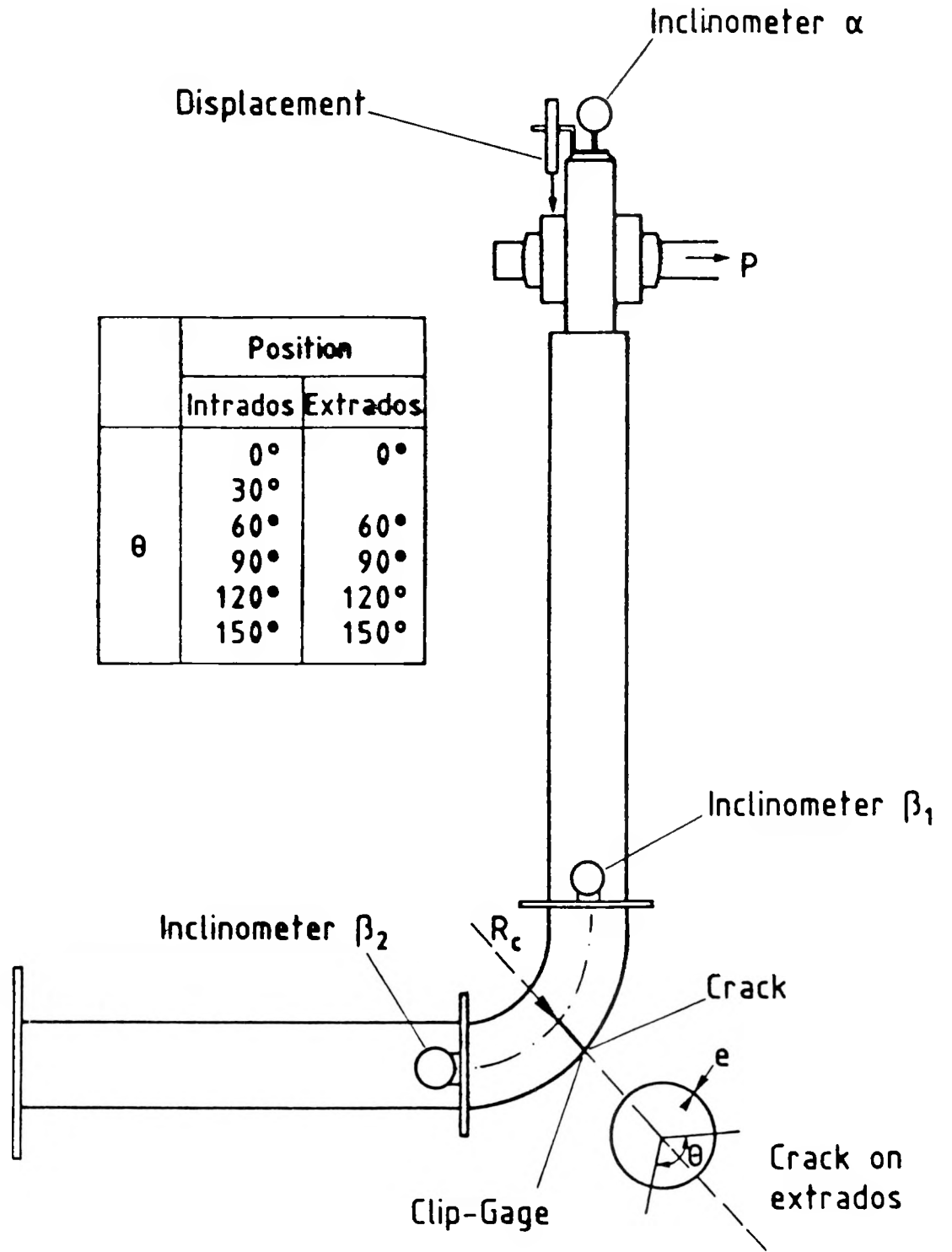


Fig. 6 - INSTRUMENTATION ON ELBOWS

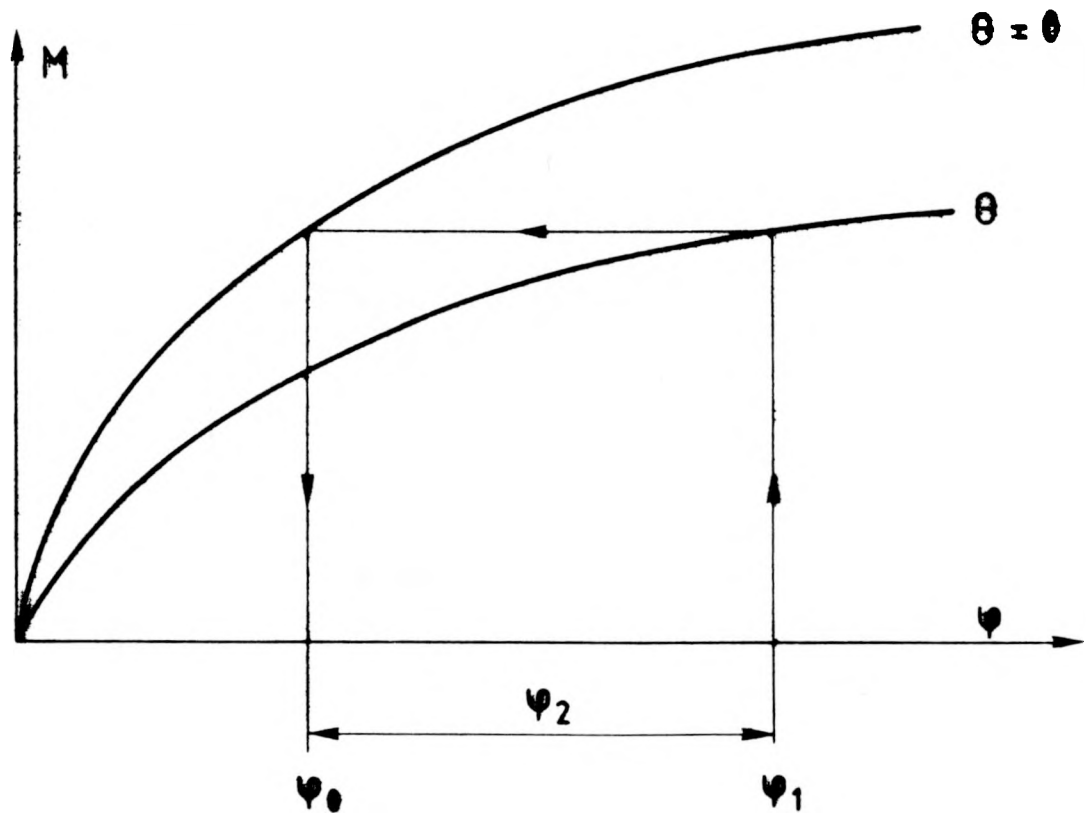


Fig. 9 - DEFINITION OF ROTATION ψ_2 DUE TO CRACK IN THE ELBOWS

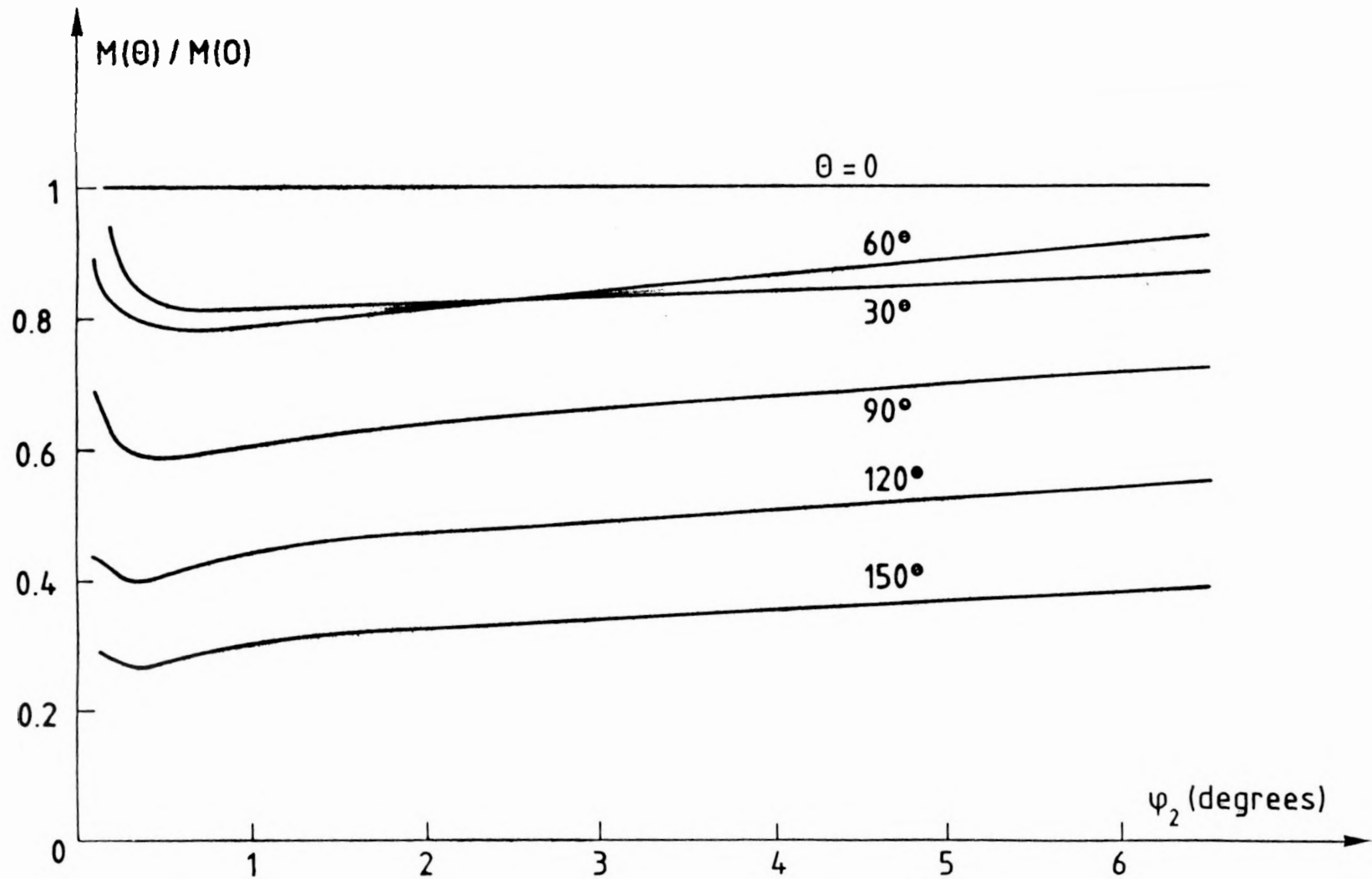


Fig. 14 - FONCTION h WITH ROTATION ψ_2 FOR TUBES

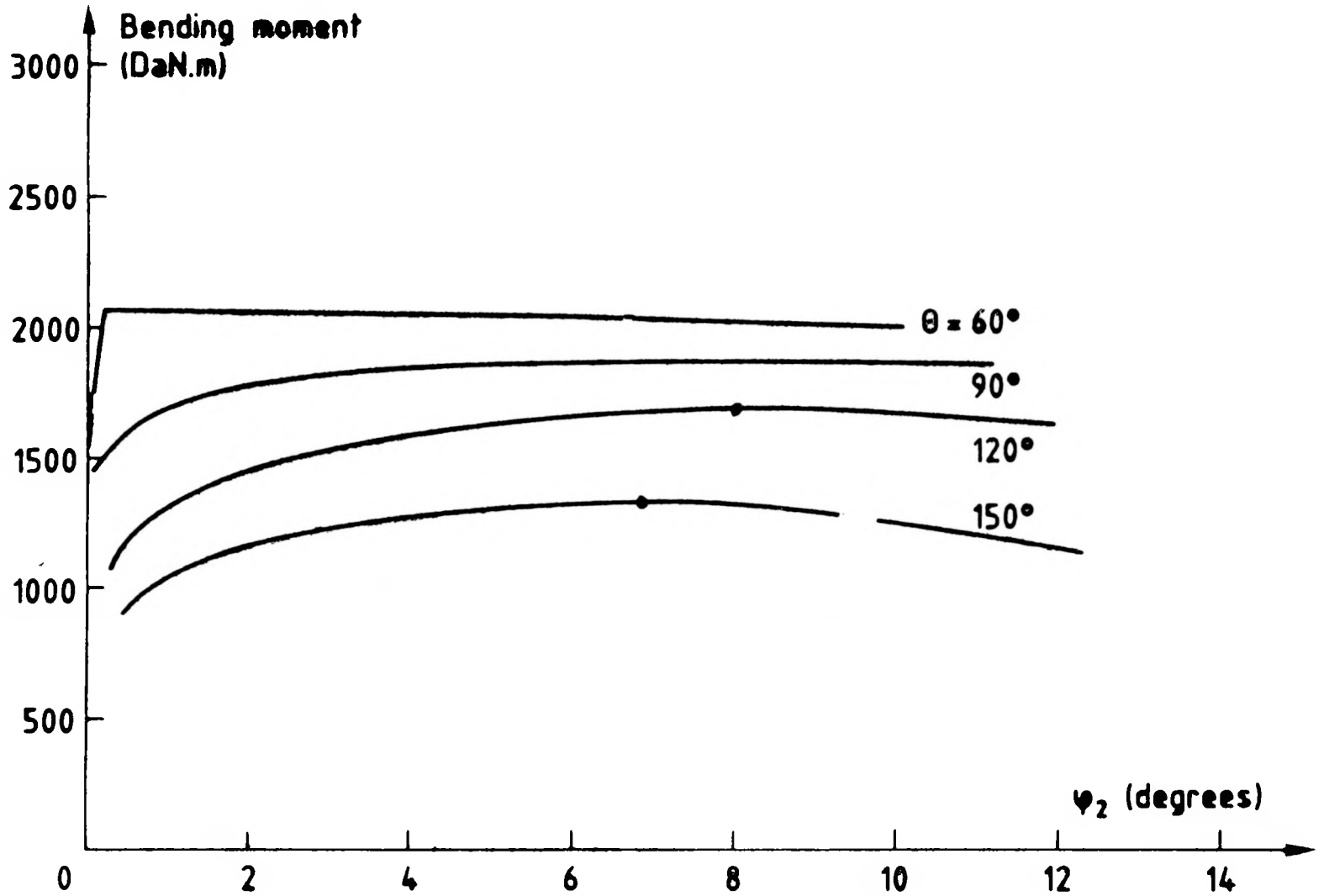


Fig. 10 - BENDING MOMENT - ROTATION ψ_2 CURVES FOR ELBOWS IN CLOSING MODE

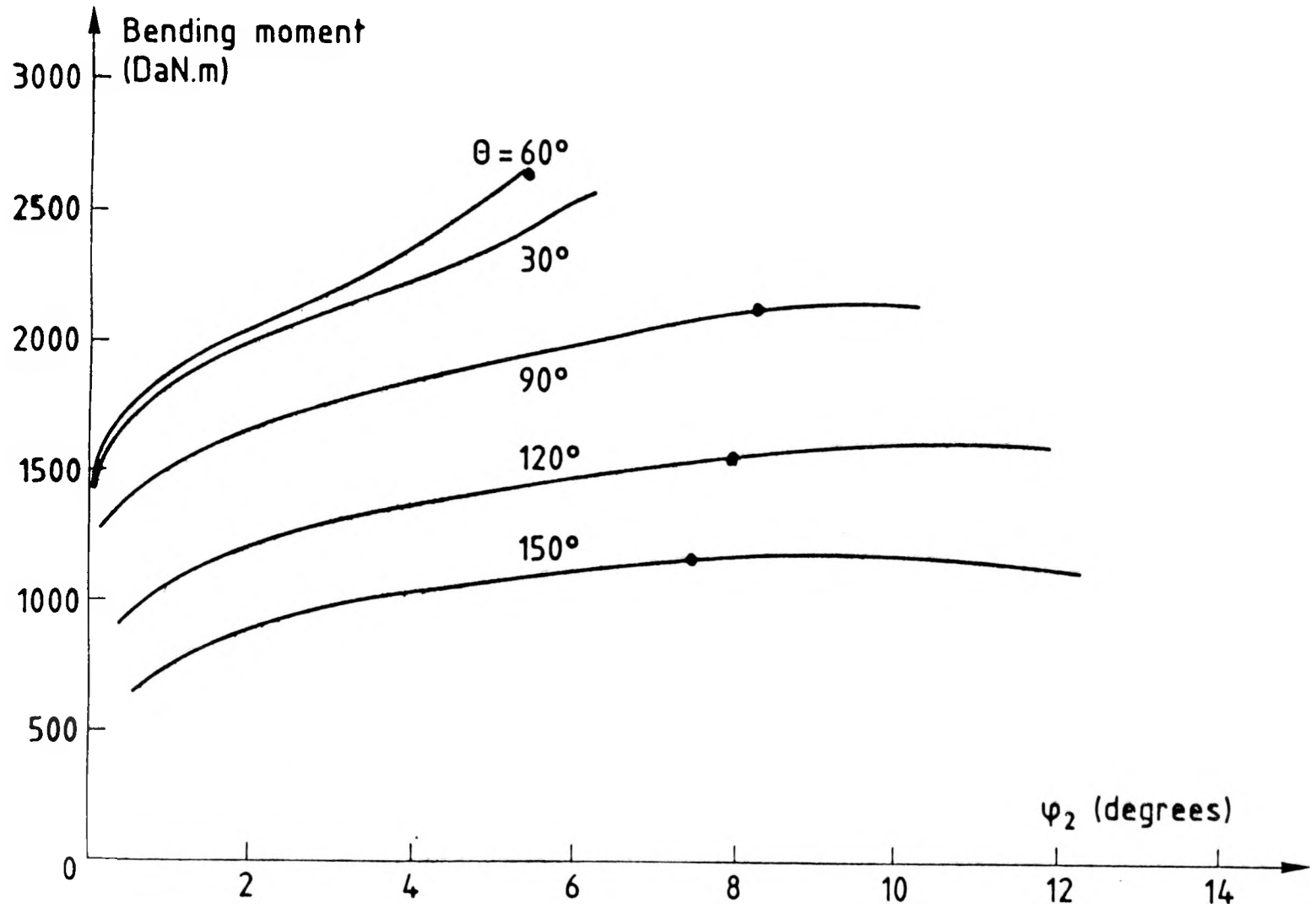
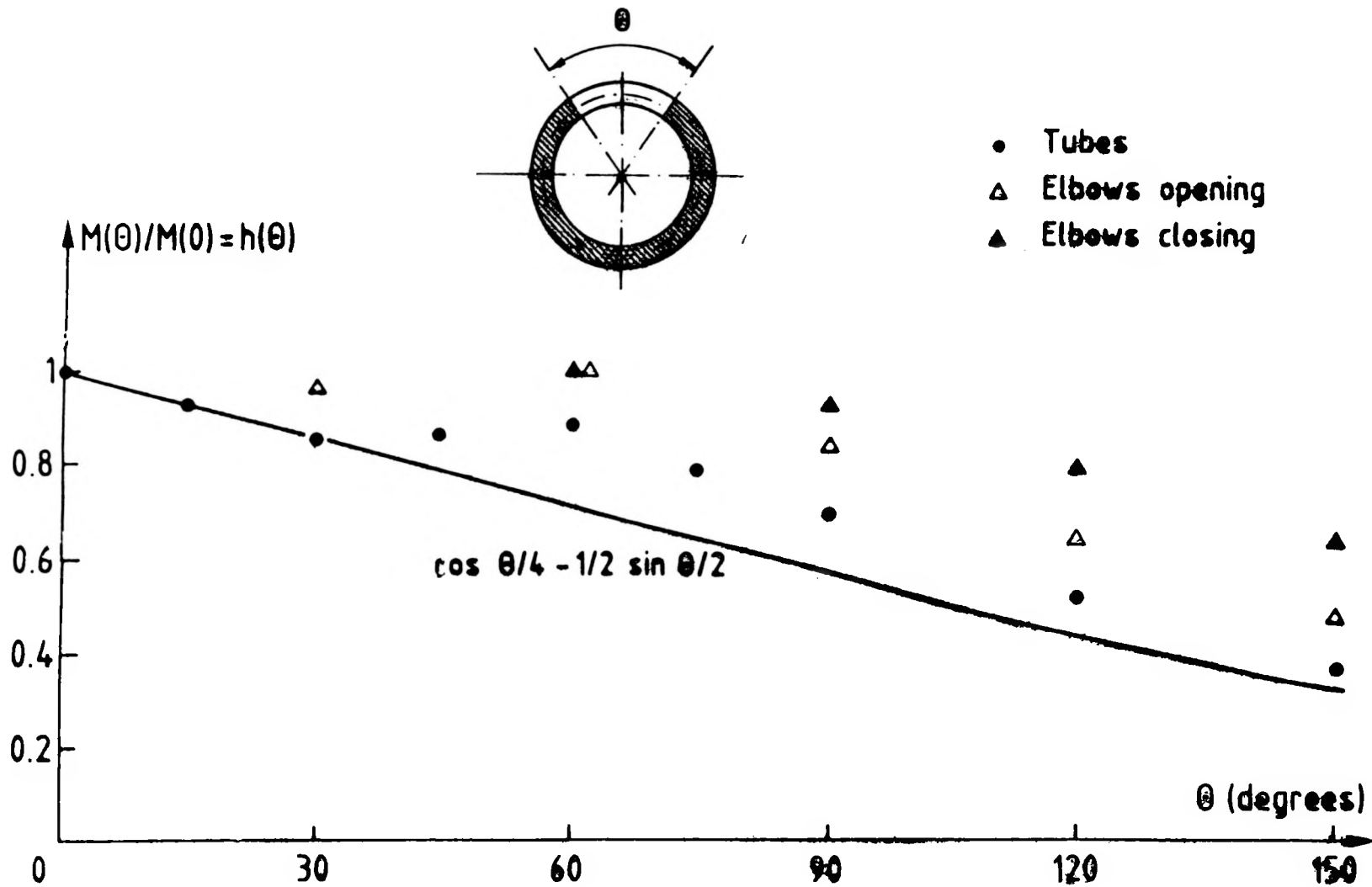


Fig. 9 - BENDING MOMENT-ROTATION ψ_2 CURVES FOR ELBOWS IN OPENING MODE



LIMIT STRESS FOR DIFFERENT COMPONENTS
(ADJUSTED FOR $\theta=0$)

MAXIMAL EXPERIMENTAL BENDING MOMENT

FORMULA : $S_{LIMIT} = (M_{MAX} / 4r^2) \times (1/B_2)$

MATERIAL FLOW STRESS : $(S_y + S_u) / 2$

- TUBES ($B_2=1$)

• $\theta \geq 60^\circ$ $S_{LIMIT} = 0.86 S_{FLOW}$

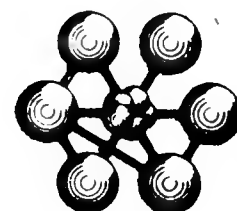
• $\theta < 60^\circ$ $S_{LIMIT} = 0.75 S_{FLOW}$

- ELBOWS ($B_2=2.1$) CLOSING MODE

• $\theta \geq 90^\circ$ $S_{LIMIT} = 1.43 S_{FLOW}$

• $\theta < 90^\circ$ $S_{LIMIT} = 1.04 S_{FLOW}$

(UPPER FORMULA NOT VALID FOR
ELBOWS IN OPENING MODE)



Longitudinal stress : $S/(m/z)$

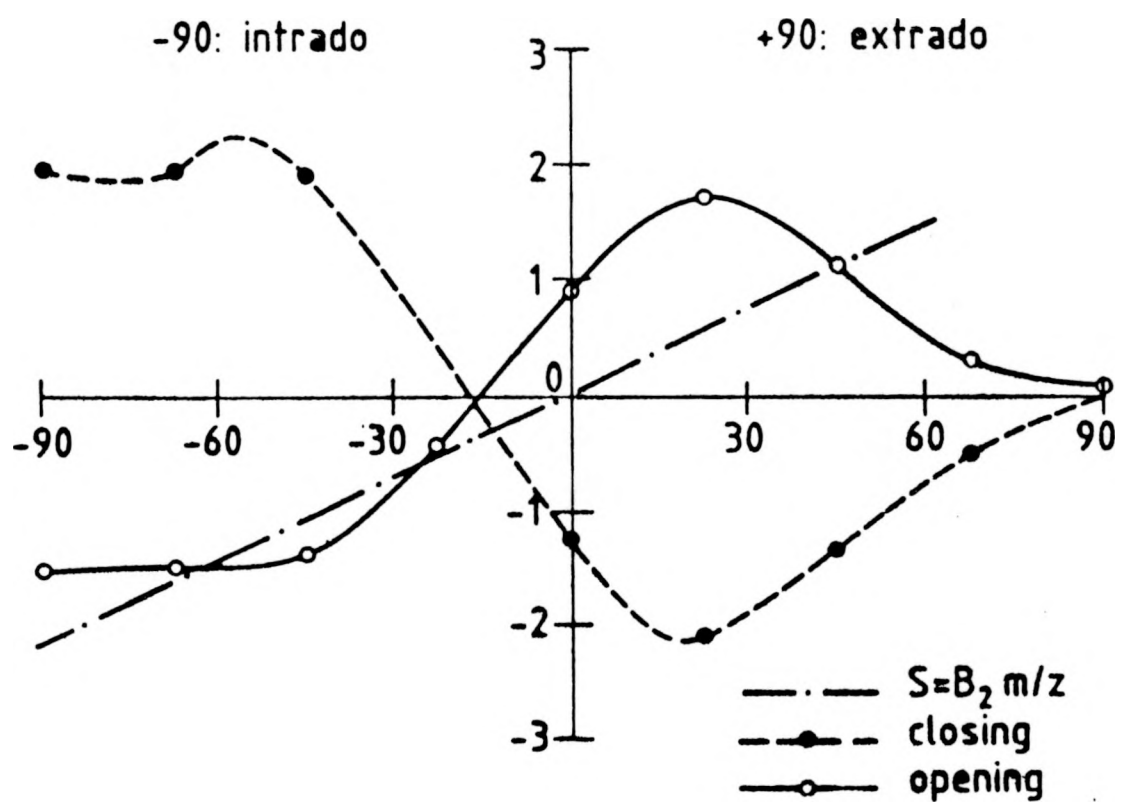
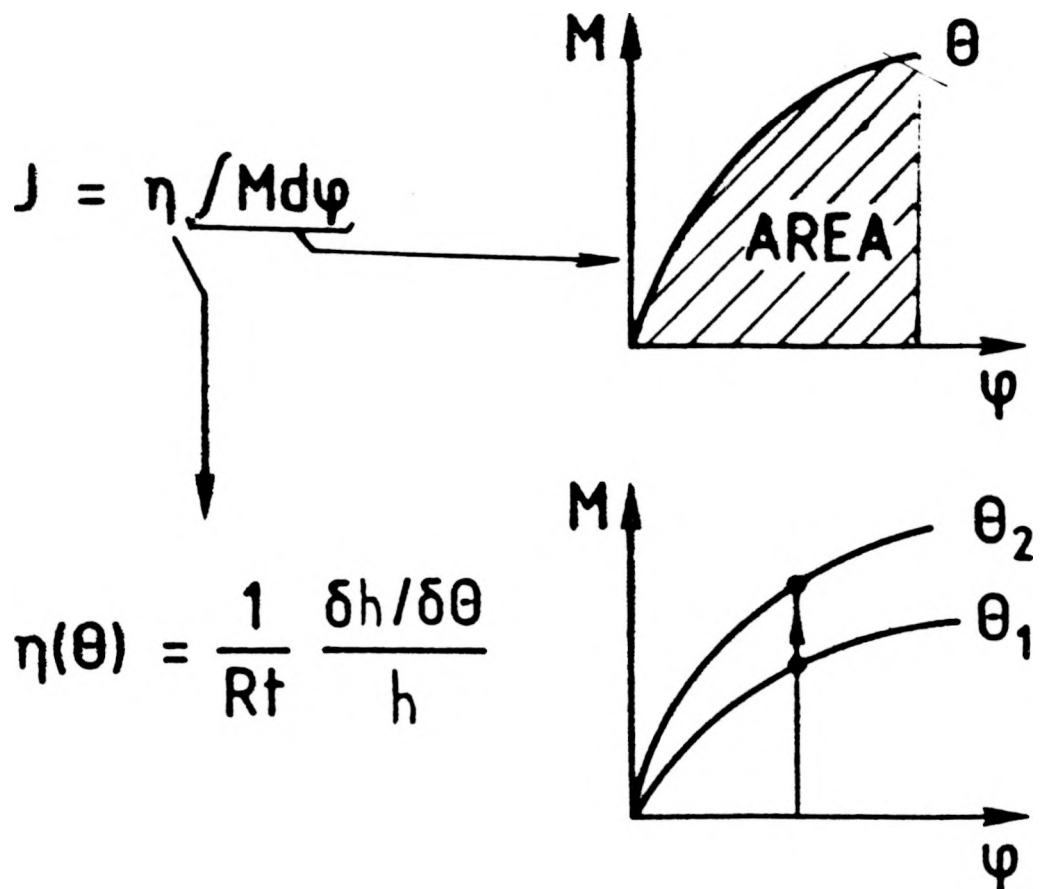


Fig. 5

EXPERIMENTAL EVALUATION OF THE DRIVING FORCE J AT INITIATION (θ CONSTANT)

BASIC METHOD

ONE SPECIMEN PROCEDURE



h SCALE FUNCTION ON BENDING
MOMENT M

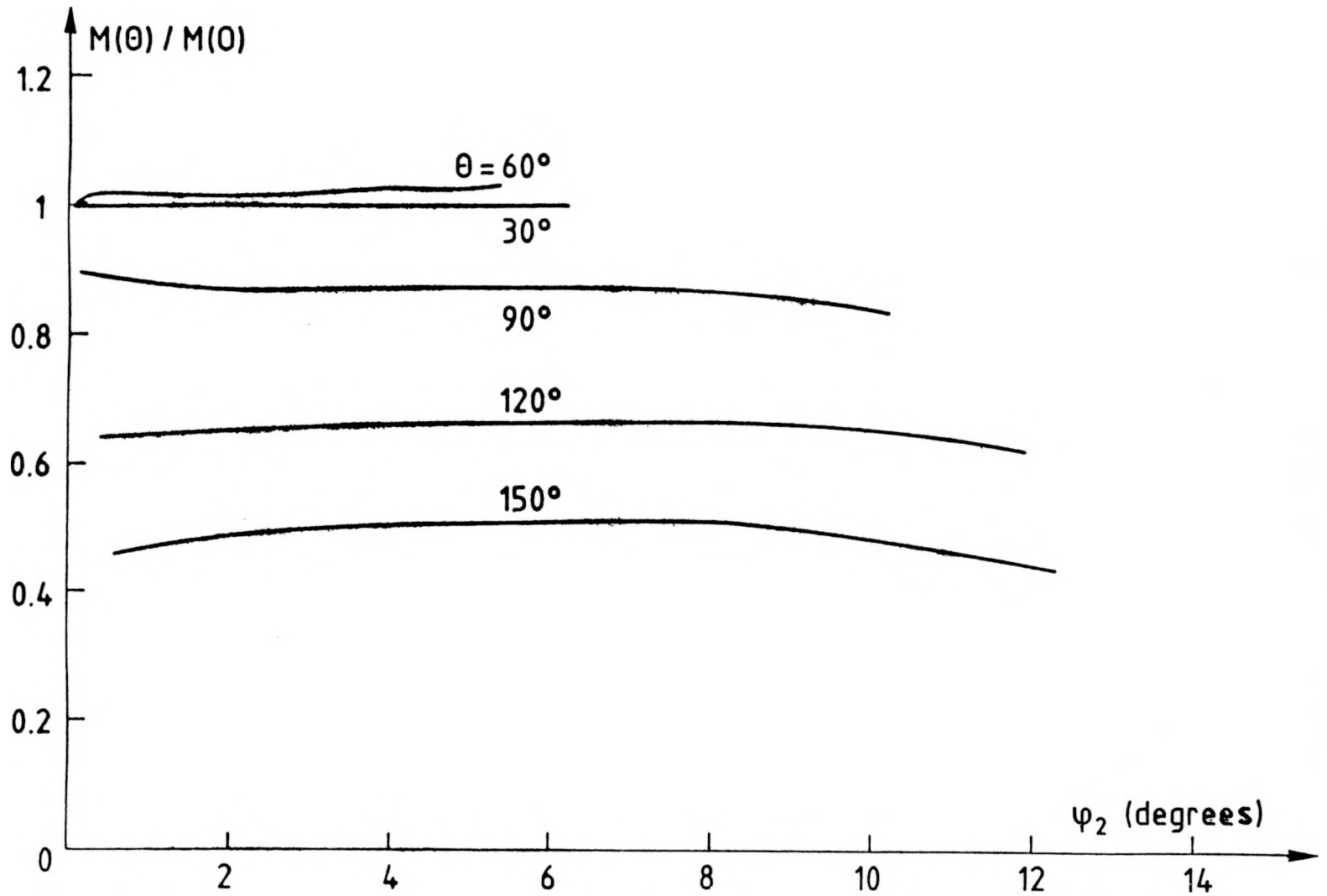


Fig. 15 - FONCTION h WITH ROTATION φ_2 FOR ELBOWS IN OPENING MODE

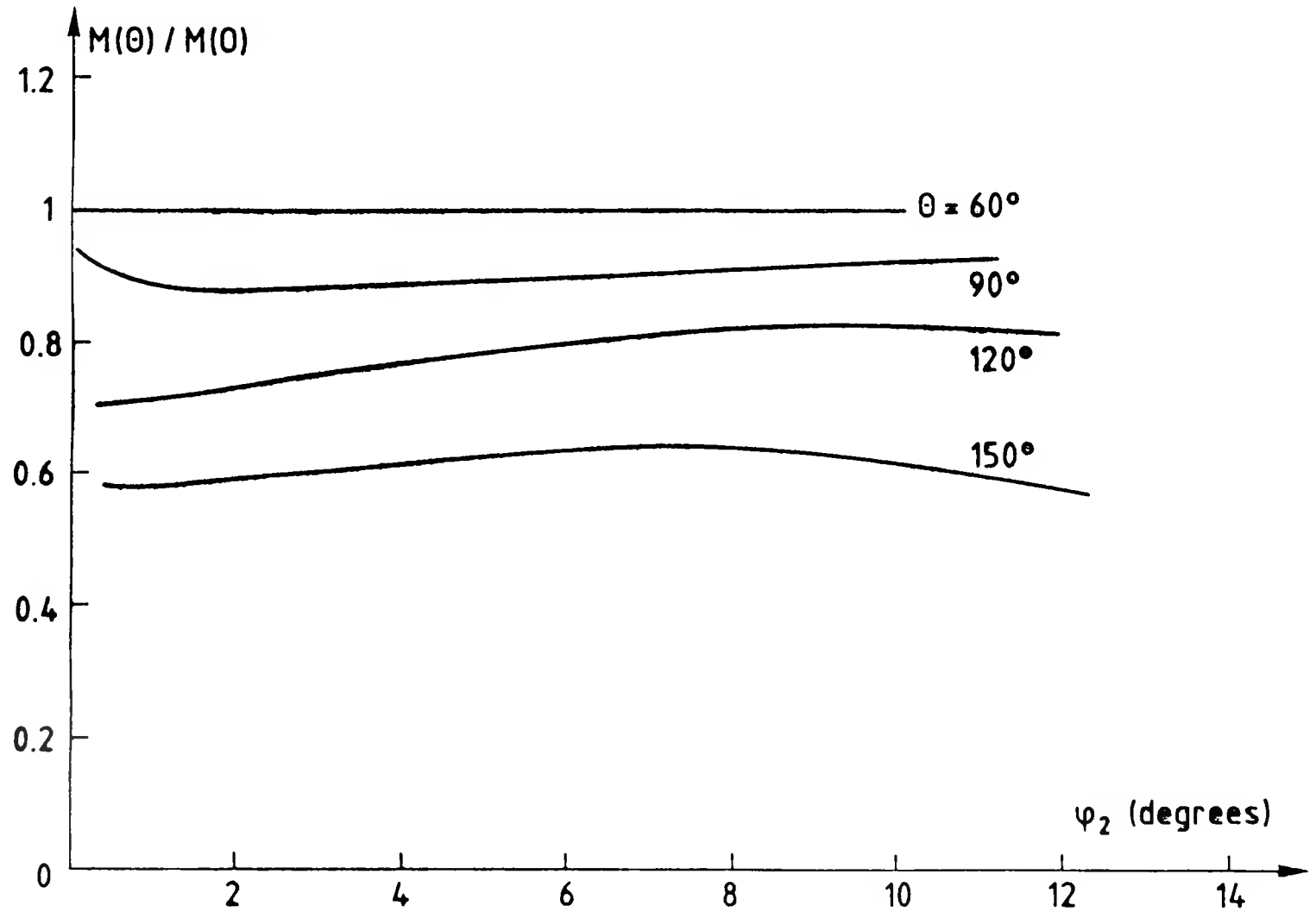
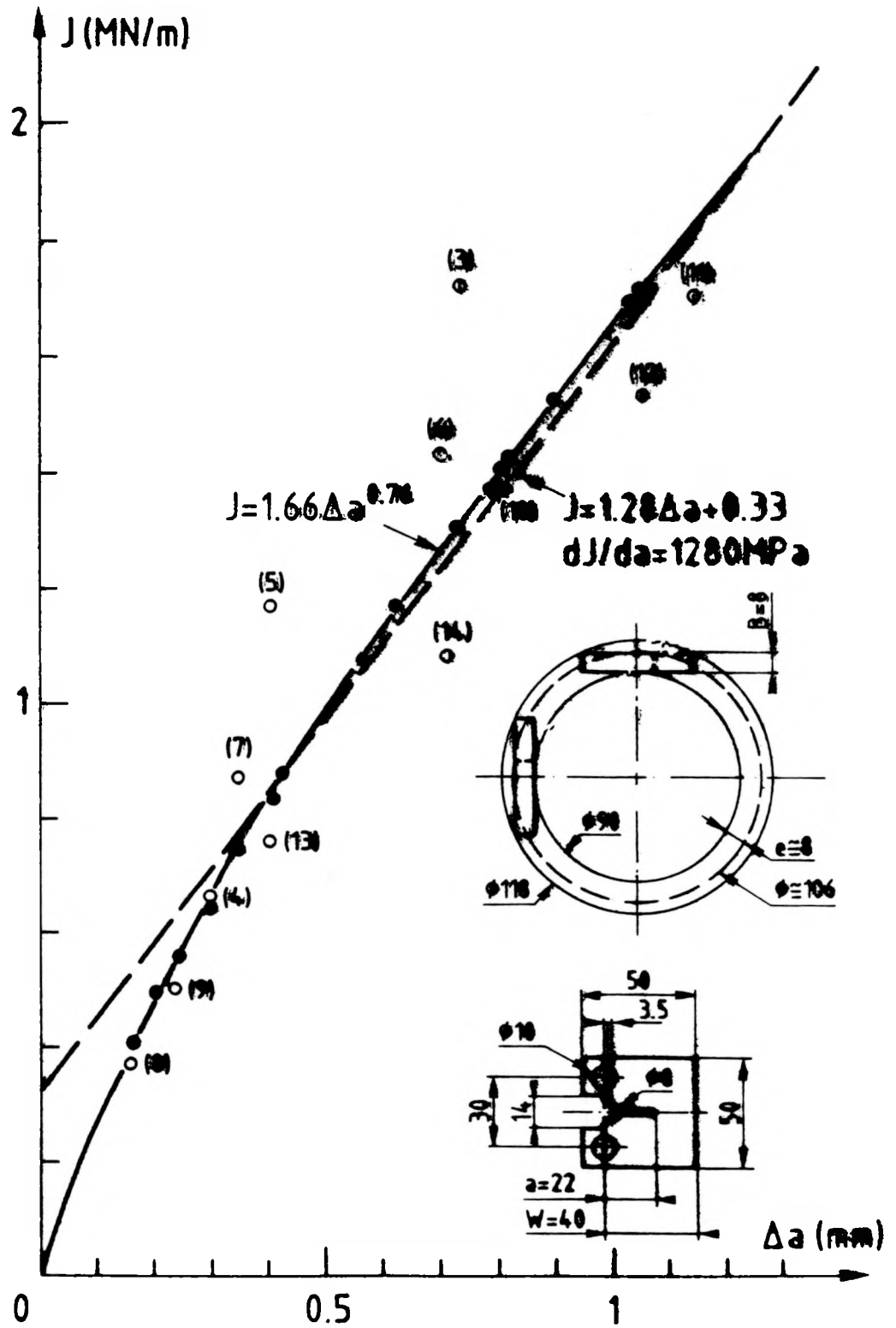


Fig. 16 - FONCTION h WITH ROTATION φ_2 FOR ELBOWS IN CLOSING MODE



J-R CURVE FROM CT20 (THICKNESS = 8mm)
ASTM E 813

ESTIMATION OF EXPERIMENTAL
J VALUE AT INITIATION
(MEAN VALUE)

- TUBES $J_1 = 1.4 \text{ MN/m}$

- ELBOWS (OPENING) $J_1 = 1.93 \text{ MN/m}$

- ELBOWS (CLOSING) $J_1 = 1.48 \text{ MN/m}$

- CT 20 SPECIMEN (FROM TUBES)

$\Delta a = 0 \text{ mm}$ $J = 0.33 \text{ MN/m}$
(EXTRAPOLATED)

$\Delta a = 1 \text{ mm}$ $J = 1.6 \text{ MN/m}$

C O N C L U S I O N S

- EXPERIMENTS
 - INITIATION CLOSE TO MAXIMUM BENDING MOMENT (FULL PLASTICITY)
 - TWO KINDS OF BEHAVIOR -- SHORT CRACKS ($0 < 60^\circ$) AND LONG CRACKS ($0 > 60^\circ$)

- LIMIT ANALYSIS
 - S_{LIMIT} RANGING FROM 0.75 TO 1.43 TIMES THE MATERIAL FLOW STRESS
 - EFFECT OF CRACK LENGTH

- EXPERIMENTAL DETERMINATION OF J AT INITIATION
 - TO TAKE INTO ACCOUNT ROTATION DUE TO CRACK
 - CORRECT COMPARISON WITH CT RESULTS
 - NO EFFECT OF CRACK LENGTH

EVALUATION OF VARIOUS CIRCUMFERENTIAL THROUGH-WALL CRACKED PIPE ESTIMATION SCHEMES

by

G. M. Wilkowski*, K. S. Chao**, F. W. Brust* and P. Gilles***

* Battelle, Columbus, Ohio U.S.A.

** Taiwan Power Company, Taipei, Taiwan, Republic of China

*** Framatome, Paris, France

ABSTRACT

During the course of the NRC's Degraded Piping Program, numerous experiments were undertaken to evaluate the fracture behavior of circumferential through-wall cracked pipe in bending or combined pressure and bending. A parallel effort involved developing a computer code with different J-integral engineering fracture mechanics methods for elastic-plastic fracture mechanics analyses. These pipe fracture data were used to assess the various estimation schemes and methods to extrapolate J-R curves for the predictions of loads and displacements of the cracked pipe.

The results show that for maximum load predictions, there are three different approaches that give similar results which are reasonably accurate yet slightly conservative. These approaches are a power-law extrapolation of the deformation J-R curve together with either the LBB.GE or LBB.ENG estimation schemes, or the linearly extrapolated modified J-R curve with the GE/EPRI method.

**Evaluation of Various Circumferential
Through-Wall Cracked Pipe Estimation Schemes**

by

G. M. Wilkowski*, F. W. Brust*, K. S. Chao
and P. Gilles*****

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- * Battelle**
- ** Taiwan Power Company**
- *** Framatome**

IS-5/89-1-GW

Objective

**Evaluate several different J-integral EPFM
Analyses for circumferential TWC pipe in
four-point bending.**

Approach

16 Circumferential TWC Pipe Fracture Experiments Evaluated

- 4 stainless steel base metal
- 5 carbon steel base metal
- 4 stainless steel weld
- 3 carbon steel weld

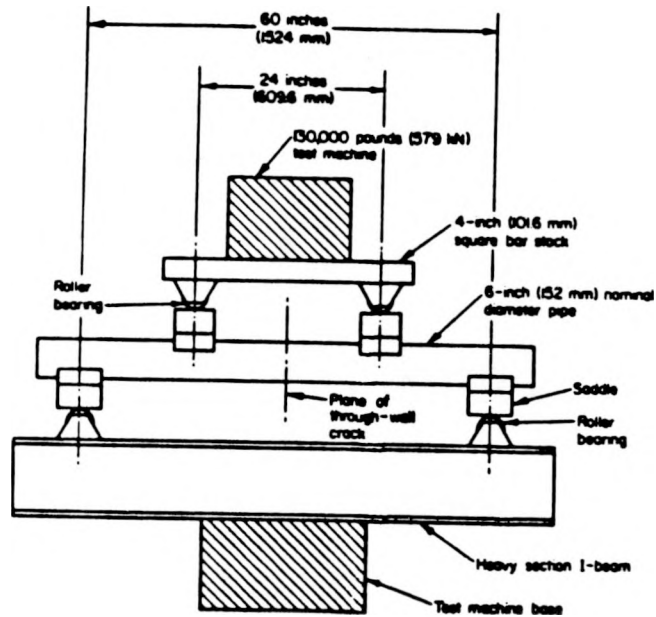
13 Degraded Pipe Program (NUREG/CR-4082)

2 EPRI (NP-2347)

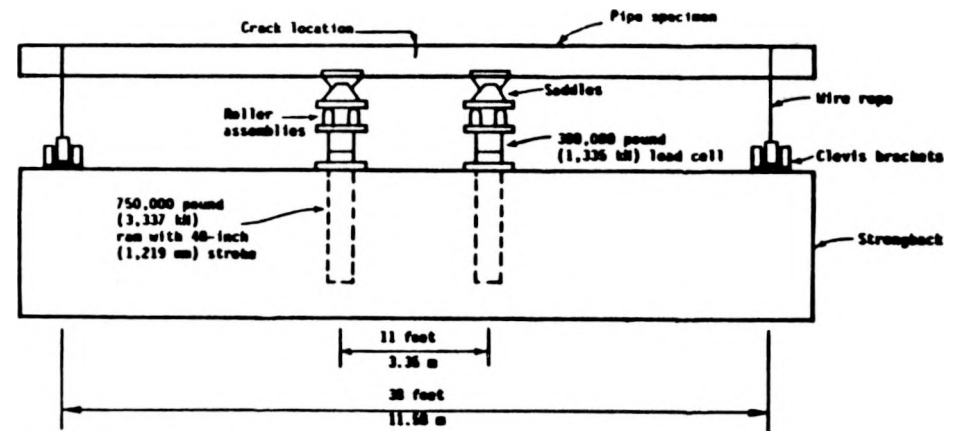
1 DTRC (NUREG/CR-3740)

Pipe Test Frames

131



(a) For small diameter pipe experiments



(b) For large diameter pipe experiments

Typical Instrumentation in Pipe Tests

- **Temperature (typically 288 C)**
- **Total load**
- **Load-line displacement**
- **Crack-opening displacement**
- **d-c electric potential
(crack initiation and growth)**
- **Pipe Rotation (sometimes)**
- **Pipe Ovalization (sometimes)**

Material Characterization

DP3II

- **Chemical analyses**
- **Charpy (ferritic)**
- **Tensile tests (R_m, 150 C, 288 C)**
- **J-R Curve**
 - **C(T) with 80% pipe thickness**
 - **L-C orientation**
 - **With and without sidegrooves**
 - **Fatigue and sharp machine notch**
 - **J_D (E813, E1152) and J_M**

Types of Analyses

- **GE/EPRI**
- **NUREG/CR-3464 (Paris)**
- **LBB.NRC**
- **LBB.GE (LBB.BCL1)**
- **LBB.ENG (LBB.BCL2)**
 - **ENG 1** numerical integration
 - **ENG 2** closed form solution

GE/EPRI

- **Original NP-1931**
- **No plastic-zone correction in J_e**
- **Modified α in J_p
NP-4883M**

GE/EPRI Analyses

J and Load Calculations

$$J = J_e + J_p$$

$J_e = \text{fcn } (R, t, \text{ crack geometry, plastic-zone correction})$

$$a_e = a + \phi r_y$$

$$\phi = 1/[1 + (P/P_0)^2]$$

$$r_y = (1/\beta \pi) [(N-1)/(N+1)] (K_1/\sigma_0)^2$$

$$J_p = \alpha \sigma_0 \epsilon_0 C (a/b) h_1 (P/P_0)^{N+1}$$

GE/EPRI Analysis

Rotation of Pipe Due to the Crack

$$\begin{aligned}\phi_c &= \phi_e + \phi_p \\ \phi_e &= (4 \sigma/E)V_3 \\ \phi_p &= \alpha \epsilon_0 h^4 (P/P_0)N\end{aligned}$$

Ramberg-Osgood Relation

Original Ramberg-Osgood

$$\varepsilon = \sigma/E + \sigma^N/F$$

Normalized Ramberg-Osgood

$$\varepsilon/\varepsilon_0 = \sigma/\sigma_0 + \alpha(\sigma/\sigma_0)^N$$

Requirements to be identical

$$E = \sigma_0/\varepsilon_0$$
$$\alpha = \sigma_0^N/(\varepsilon_0 F)$$

Modified GE/EPRI
(NP-4883m)

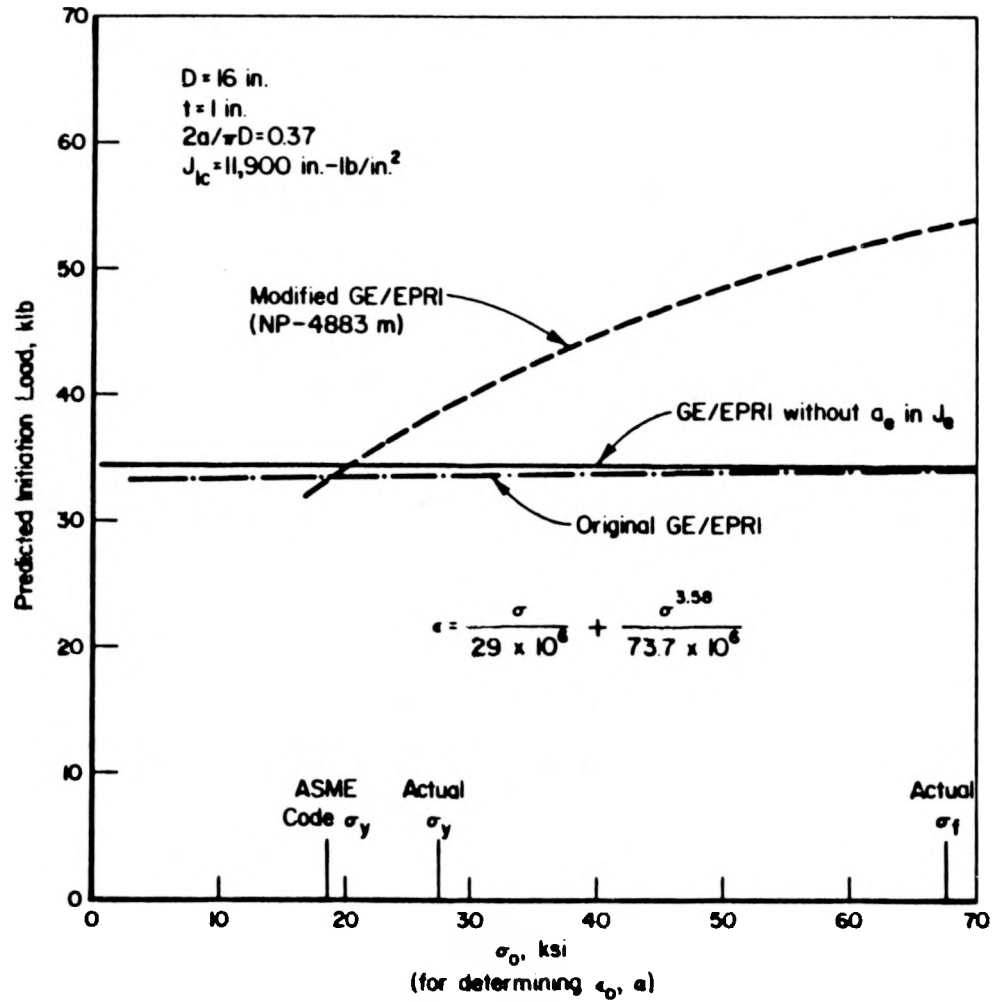
$$J_p = \alpha [1/(N+1)] \sigma_0 \epsilon_0 C \cdot (a/b) \cdot h_1 (P/P_0)^{N+1}$$

Not unique for σ_0 = different values of identical tensile test data.

Modification of NP-4883m

$$J_p = \{ \alpha_0 (\sigma_0/\sigma_y)^{[(N-1)n]} \} [1/(N+1)] \sigma_0 \epsilon_0 C \cdot (a/b) \cdot h_1 (P/P_0)^{N+1}$$

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NUREG/CR-3464

Step 1 Calculate $M-\phi_c$ curve

- Interpolates between linear-elastic and fully plastic solution

$$M = [E \pi R^2 t / I(\theta_{\text{eff}})] \phi_e^c$$

$$\theta_{\text{eff}} = \theta_0 + \theta_y$$

$$\theta_y = K_1^2 / (\beta \pi R_m \sigma_y^2)$$

β adjusted so $M_{\text{max}} = M$ from limit load

$$\phi_e^c = \frac{\partial}{\partial M} \int K_i^2 / E \, dA$$

$$K_1 = \sigma F(\theta) \sqrt{\pi R_m \theta}$$

NUREG/CR-3464

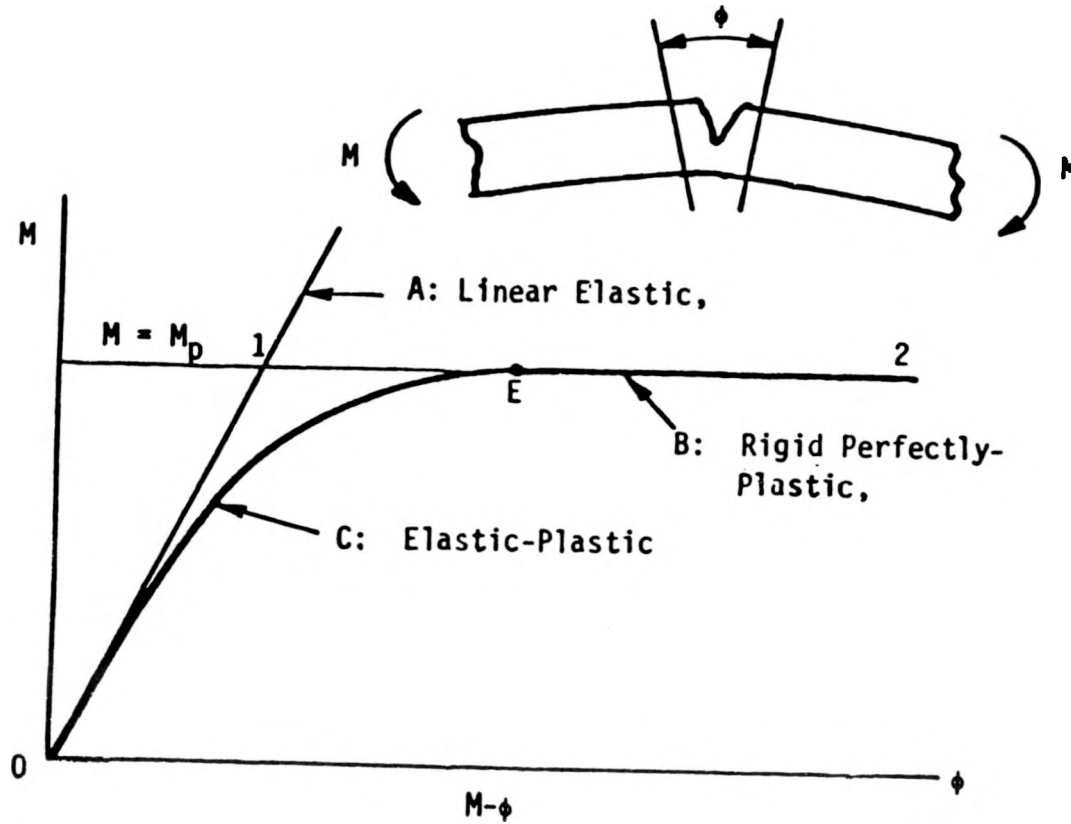
Step 2 Calculate J from M- ϕ_c curve

$$J = J_e + J_p$$

$$J_e = K_1^2/E$$

$$J_p = \{ \sigma_f R [\sin(\Theta/2) + \cos\Theta] / [M^{RP}(\Theta)] \} \int_0^{\phi_c} M(\Theta) d\phi_p$$

$$M^{RP} = 4 \sigma_f R^2 t \{ \cos(\Theta/2) - 0.5 \sin \Theta \}$$



LBB.NRC

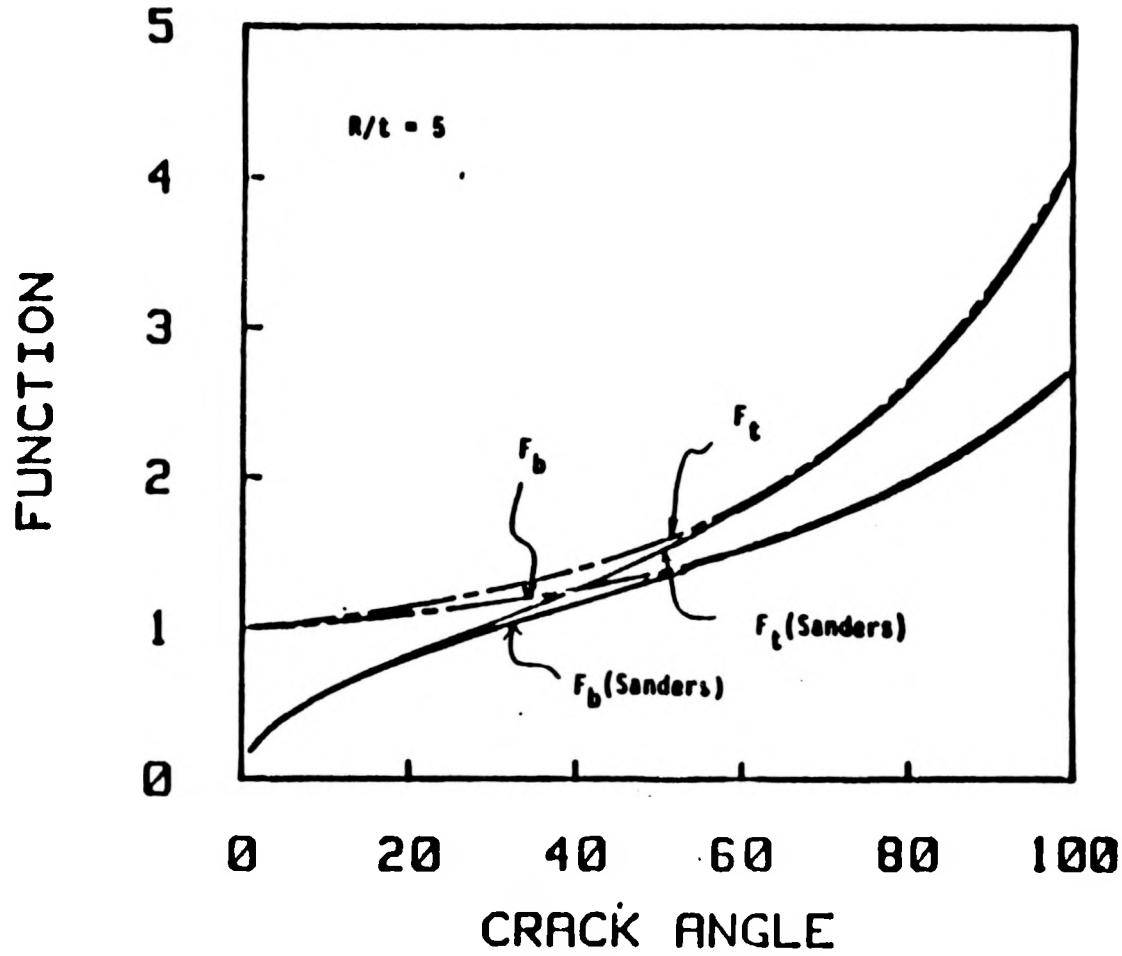
Same as NUREG/CR-3464 except

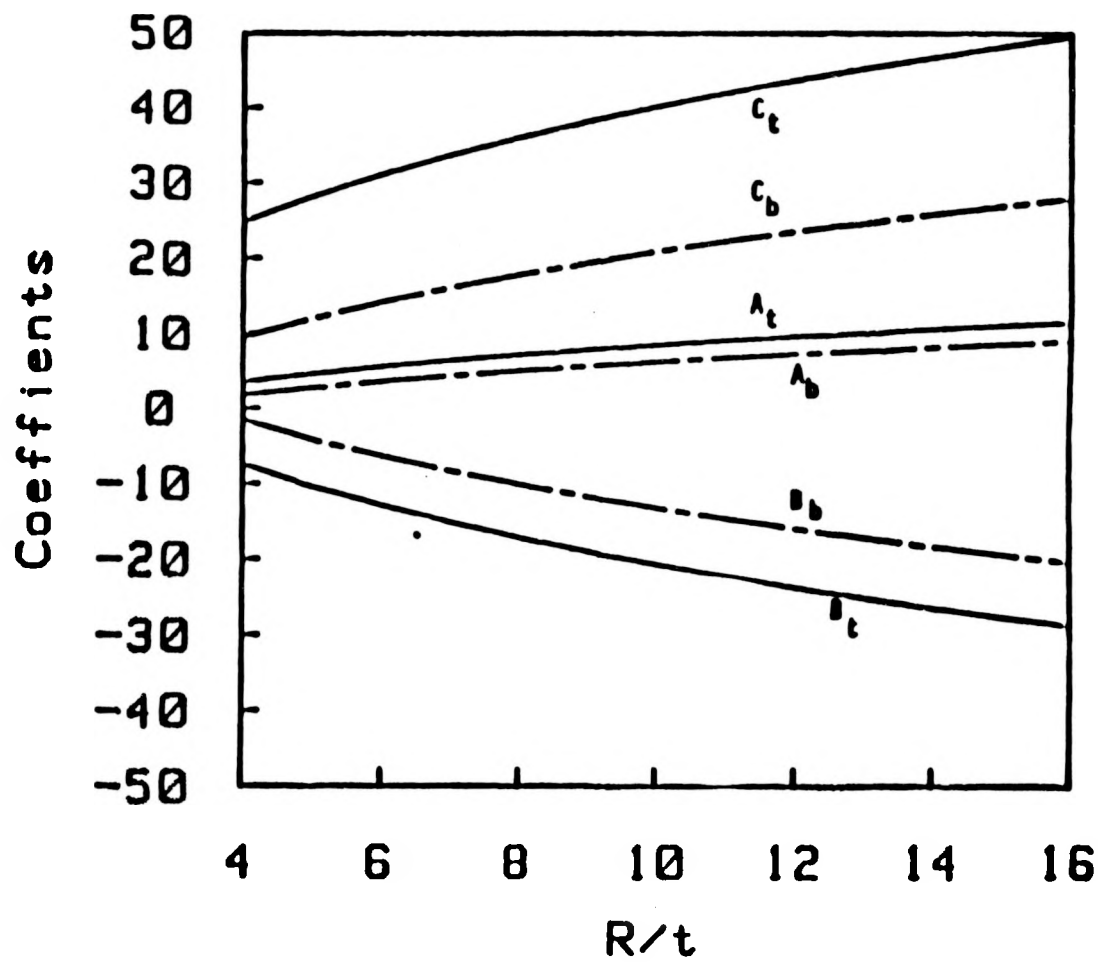
$$\phi = \phi_e + \phi_p$$

$$\phi_e = \frac{\partial}{\partial M} \int K_i^2 / E \, dA$$

$$\phi_p = \alpha (\sigma / \sigma_f)^{N-1} \phi_e$$

Sanders F-functions expanded





LBB.BCL1 (LBB.GE) Method

Step 1 $M-\phi_c$ curve determined from GE functions and no plastic-zone corrections

$$\phi_c = \phi_e + \phi_p$$

$$\phi_e = f_4(M/E) \text{ and } \phi_p = \alpha \epsilon_0 h_4 (M/M_0)^N$$

(h_4 function not accurate for crack angles < 30 degrees)

Step 2 Calculate J from $M-\phi_c$ relation

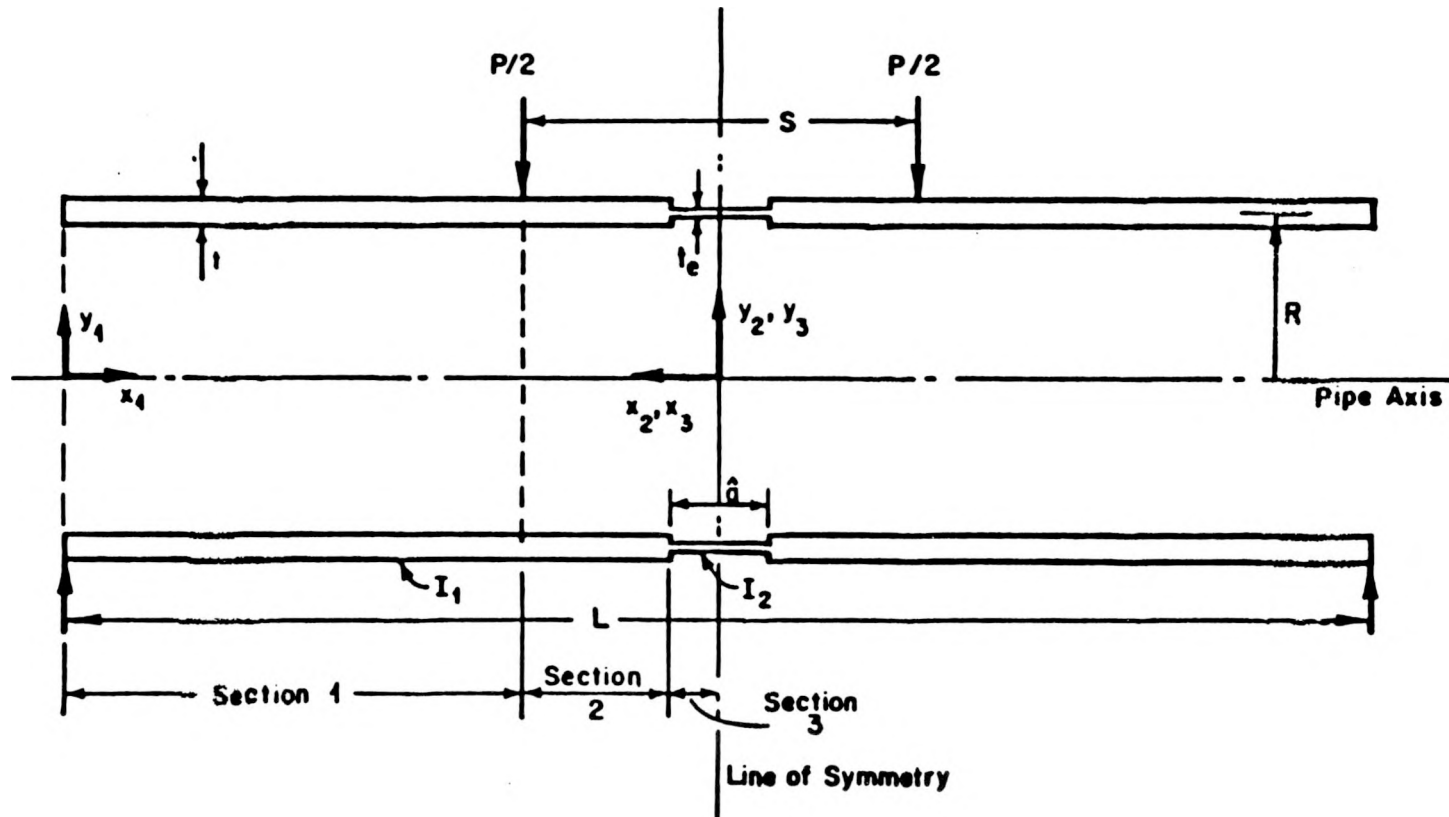
LBB.BCL2 (LBB.ENG)

Step 1 $M-\phi_c$ curve determined using reduced stiffness engineering approach

$$\phi_{pc} = (t/t_e)^{N-1} (\pi/4\hat{K})^N \alpha (\sigma/\sigma_f)^{N-1} \phi_{ec}$$
$$t_e = t[\cos(\Theta/2) - 0.5 \sin \Theta]$$

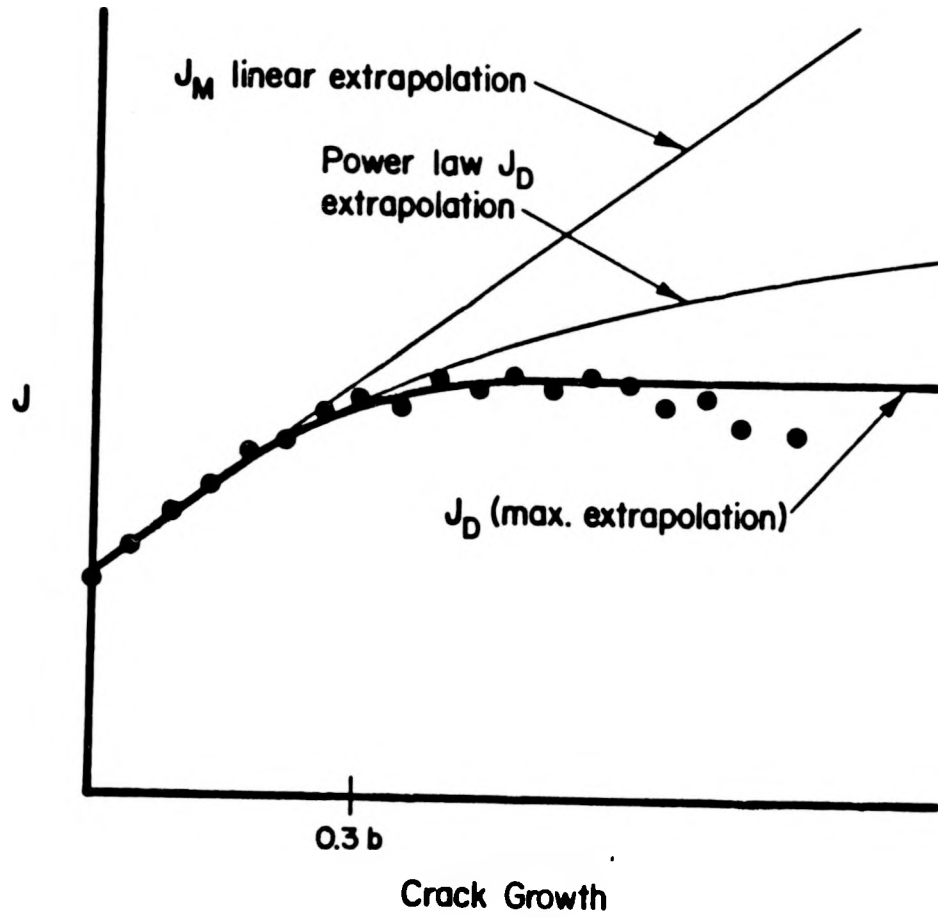
\hat{K} given in NUREG/CR-4853

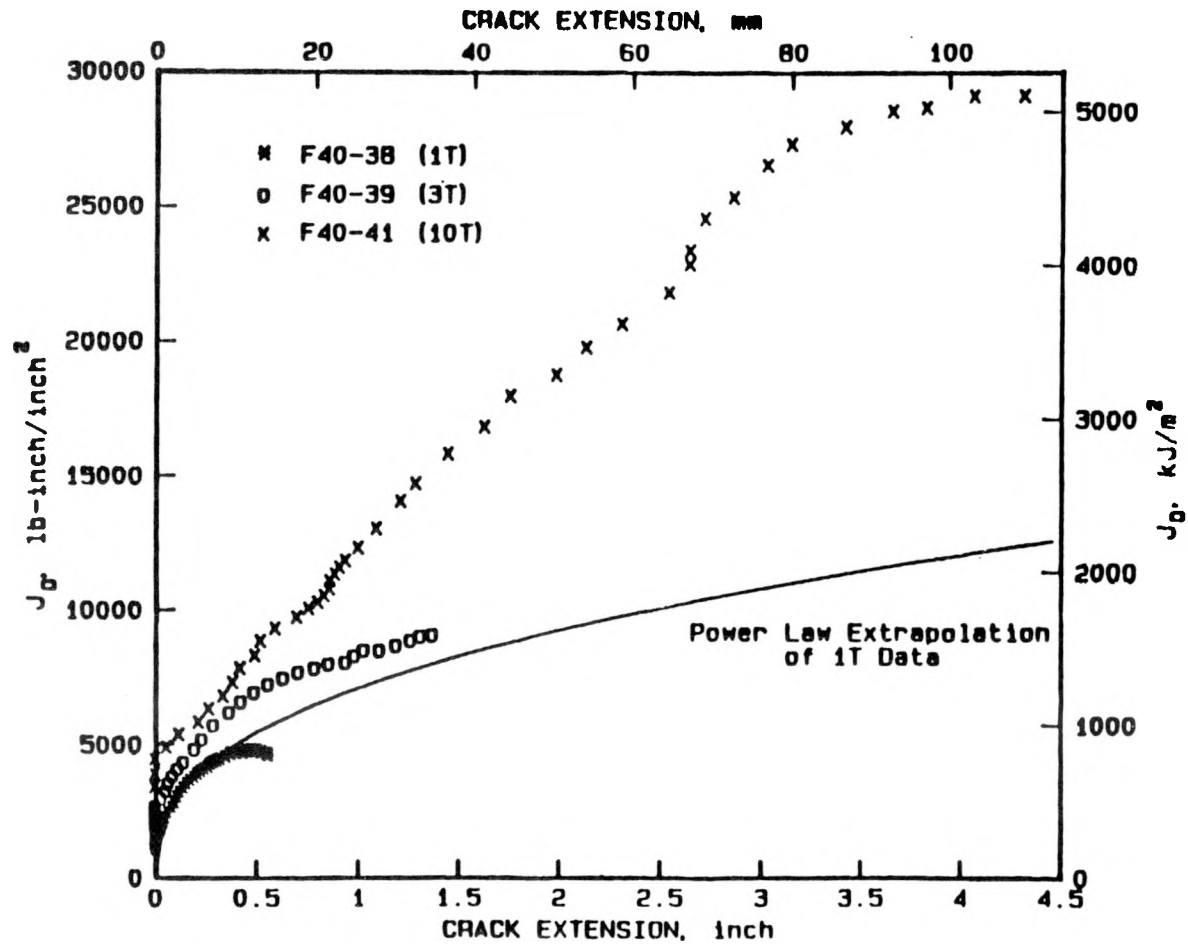
Step 2 Calculate J from $M-\phi_c$ relation

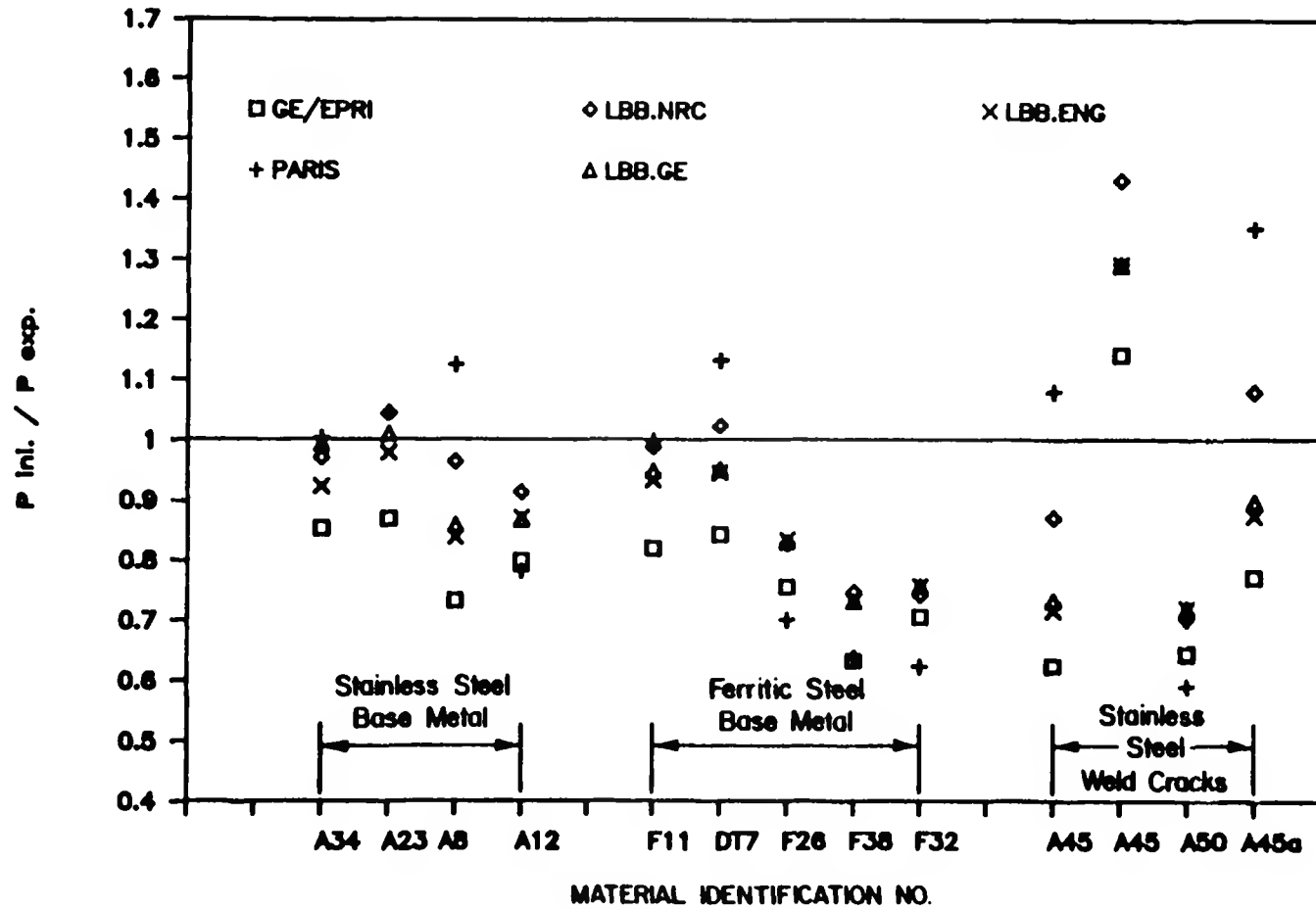


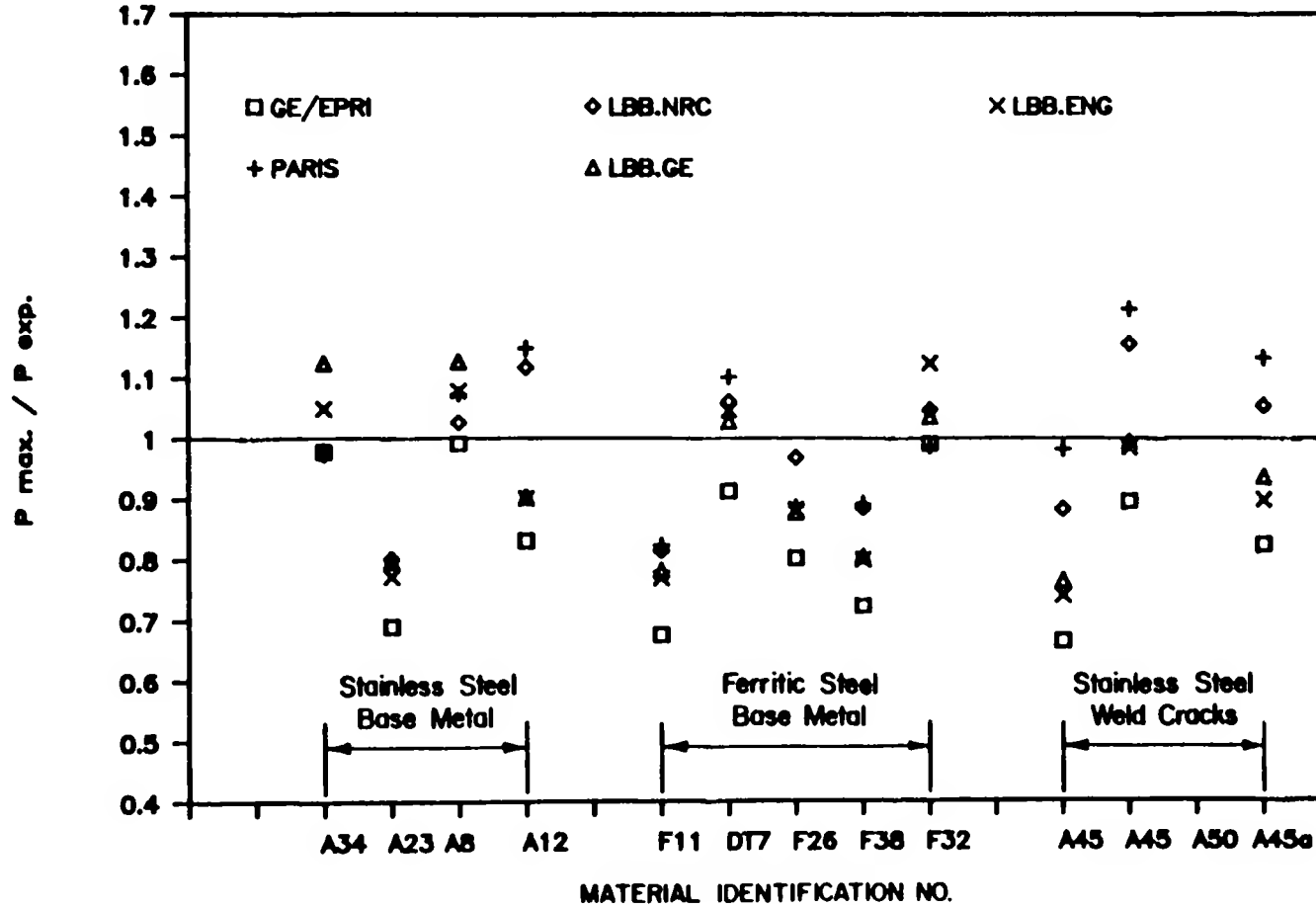
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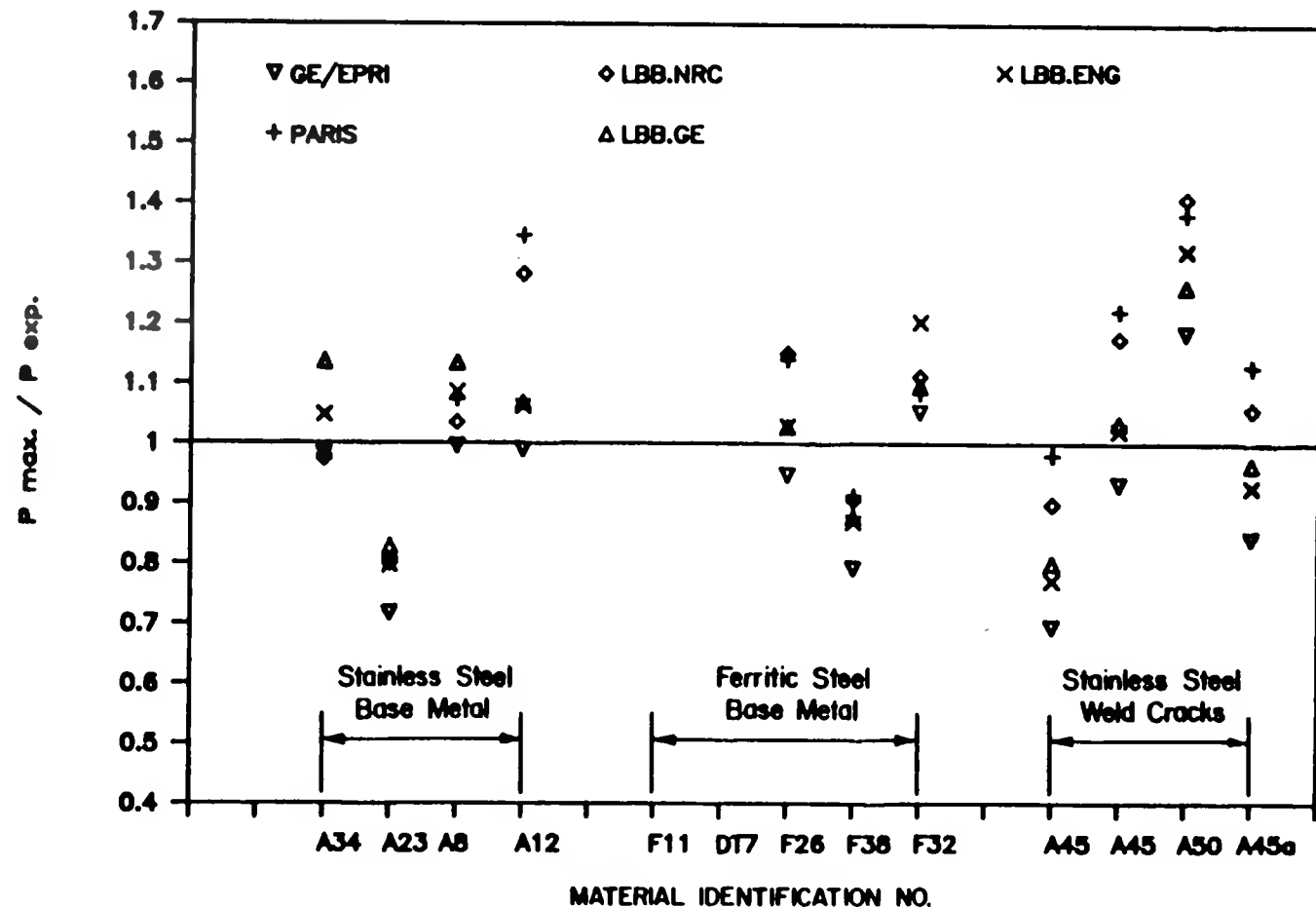


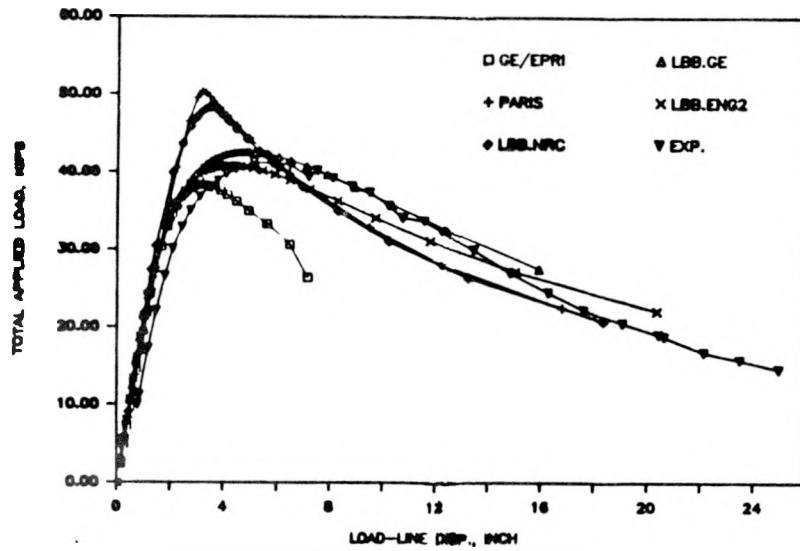




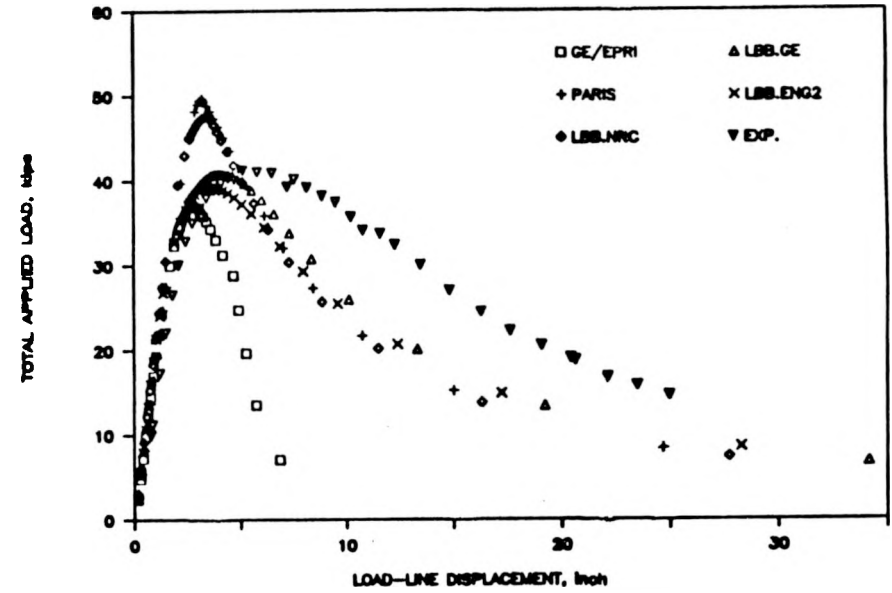


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(a) Using linearly extrapolated J_M -R curve



(b) Using power-law extrapolated J_D -R curve

Summary of Pipe Experiments

<u>Exp. No.</u>	O.D., <u>inch</u>	<u>Pipe No.</u>
<u>Stainless Steel Base Metal</u>		
NP-2347-6T	2.375	A34
4131-5	6.254	A23
NP-2347-8T	16.28	A8
4111-3	42.00	A12
<u>Stainless Steel Weld Metal</u>		
4141-1	6.625	A45W
4141-3	16.28	A45W
4111-5	28.33	A50W
4141-5	6.605	A45a
<u>Carbon Steel Base Metal</u>		
4111-1	4.50	F11
DTRC-7	8.63	DT7
4111-2	28.00	F26
4111-6	35.83	F38
4111-4	42.00	F32
<u>Carbon Steel Weld Metal</u>		
WJ-1	6.625	F86W
4141-9	16.00	F29W
4141-7	36.73	F38W

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Using Linear Extrapolation of J_M-R Curve

	<u>Predicted/Exp. Maximum Load</u>				
	<u>GE/EPRI</u>	<u>PARIS</u>	<u>LBB.NRC</u>	<u>LBB.GE</u>	<u>LBB.ENG</u>
<u>Stainless Steel Base Metal</u>					
Average	0.92	1.05	1.02	1.04	1.00
Standard Deviation	0.12	0.20	0.17	0.13	0.12
<u>Carbon Steel Base Metal</u>					
Average	0.93	1.05	1.06	1.00	1.03
Standard Deviation	0.11	0.10	0.11	0.09	0.14
<u>Stainless Steel Weld Metal</u>					
Average	0.91	1.18	1.14	1.02	1.01
Standard Deviation	0.18	0.15	0.19	0.17	0.20
<u>Carbon Steel Weld Metal</u>					
Average	0.98	1.09	1.04	1.13	1.13
Standard Deviation	-	-	-	-	-
Total Average	0.93	1.09	1.06	1.05	1.04
Total Standard Deviation	0.14	0.15	0.16	0.13	0.15

Using Power Law Extrapolation of J_D-R Curve

	<u>Predicted/Exp. Maximum Load</u>				
	<u>GE/EPRI</u>	<u>PARIS</u>	<u>LBB.NRC</u>	<u>LBB.GE</u>	<u>LBB.ENG</u>
<u>Stainless Steel Base Metal</u>					
Average	0.87	1.00	0.98	0.99	0.95
Standard Deviation	0.12	0.13	0.12	0.14	0.12
<u>Carbon Steel Base Metal</u>					
Average	0.82	0.94	0.95	0.90	0.92
Standard Deviation	0.12	0.10	0.09	0.11	0.14
<u>Stainless Steel Weld Metal</u>					
Average	0.79	1.11	1.03	0.90	0.87
Standard Deviation	0.10	0.10	0.11	0.10	0.10
<u>Carbon Steel Weld Metal</u>					
Average	0.84	1.02	0.99	0.96	0.94
Standard Deviation	0.06	0.06	0.03	.095	0.10
Total Average	0.83	1.02	0.99	0.94	0.91
Total Standard Deviation	0.10	0.10	0.09	0.11	0.11

Conclusions

On the Average

- **GE/EPRI method gives lowest predicted max. loads**
- **NUREG/CR-3464 method gives highest predicted max. loads**
- **Using J_M -R curve gives 9% higher predicted max. loads**
- **All methods except GE/EPRI overpredict loads using the J_M -R curve**
- **Slightly conservative but reasonably accurate predictions can be made using**
 - **LBB.GE with power law extrapolated J_D -R curve**
 - **LBB.ENG with power law extrapolated J_D -R curve**
 - **GE/EPRI with linear extrapolated J_M -R curve**

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EXPERIENCES IN APPLYING LEAK-BEFORE-BREAK TO DARLINGTON NGS

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T.C. Lin, A.S. Misra, K.R. Gilbert

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Toronto, Ontario
CANADA - M5G 1X6

ABSTRACT

Ontario Hydro has developed a leak-before-break approach for application to the large diameter heat transport piping for Darlington Nuclear Generating Station (NGS) as an alternative to the provision of pipewhip restraints. The work done by Ontario Hydro to obtain the technology and develop the specific methodology have been previously documented (see References). This approach has been applied to the large diameter piping in the heat transport system for Ontario Hydro's most recent four unit (881 MWe per unit) CANDU nuclear generating station. The piping systems are ASME Class 1 systems constructed of A106 Grade B carbon steel.

The present presentation describes the leak-before-break approach being used and experiences with the application to Darlington Nuclear Generating Station.

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- [1] Aggarwal, M.L., Kozluk, M.J., Lin, T.C., Manning, B.W., and Vijay, D.K.
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International Journal of Pressure Vessels and Piping; Vol.25 1986, pp.239-256
- [2] Kozluk, M.J., Aggarwal, M.L., Vijay, D.K.
"Demonstrating Leak-Before-Break for CANDU Heat Transport Piping"
International Journal of Pressure Vessels and Piping; Vol.34 1988, pp.255-263
- [3] Nathwani, J.S., Kee, B.L., Kim, C.S., Kozluk, M.J.
"Ontario Hydro's Leak Before Break Approach:
Application to the Darlington CANDU Nuclear Generating Station A"
Nuclear Engineering and Design; Vol.111 1989, pp.85-107

EXPERIENCES IN APPLYING LEAK-BEFORE-BREAK TO DARLINGTON NGS

**MICHAEL J. KOZLUK
ONTARIO HYDRO
TORONTO, CANADA**

MJK-IP1RQ/88

TOPICS TO BE COVERED

BACKGROUND

LBB FOR CANDU PIPING

APPLICATION TO DARLINGTON

VERIFICATION OF TECHNOLOGY

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THE ROAD TO LBB

- **CONSTRUCTION LICENSE**
 - **NUCLEAR SAFETY DESIGN GUIDE**
 - **ASSESS VIABILITY OF LBB**
 - **MANAGEMENT APPROVES USE OF LBB**
 - **AECB ACCEPTS THE USE OF LBB (IN PRINCIPLE)**
-

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LBB TASK GROUPS

- **OTHER JURISDICTIONS**
 - **LEAK DETECTION SYSTEMS**
 - **SYSTEM TRANSIENTS**
 - **WELDING, CONSTRUCTION, FABRICATION, INSPECTION**
 - **FAILURE MECHANISMS**
 - **LEAK RATE MODEL**
 - **MATERIAL TESTING**
 - **EPFM TECHNOLOGY**
-

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THE PLAN

- BUY THE TECHNOLOGY
 - DEVELOP AXIAL CRACKS
 - ACTUAL MATERIAL DATA
 - MCAE PROGRAMS
 - MINI-COMPUTER
 - GRAPHICS WORKSTATION
 - ANALYZE CASE-BY-CASE
-

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TOPICS TO BE COVERED

BACKGROUND

LBB FOR CANDU PIPING

APPLICATION TO DARLINGTON

VERIFICATION OF TECHNOLOGY

MJK-IP1RQ/88

CONDITIONS ON USING LBB

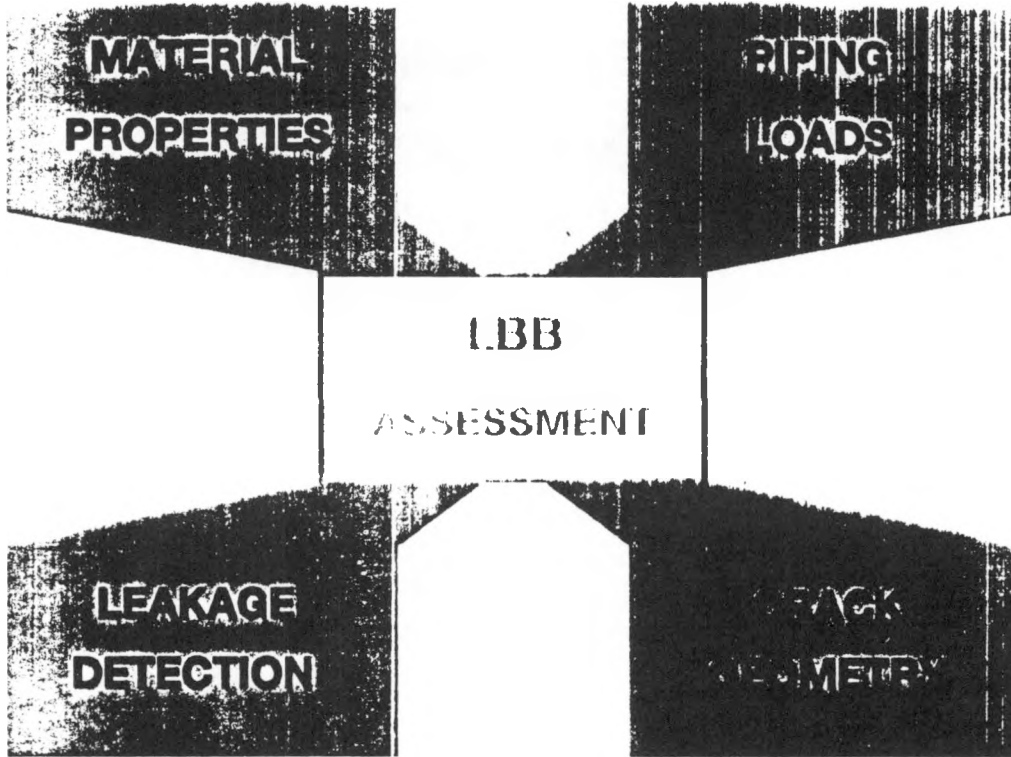
- **CONTAINMENT INTACT**
 - **PROTECT FROM PIPEWHIP**
 - **SHUTDOWN SYSTEMS**
 - **MODERATOR**
 - **ECI**
 - **PIPEWHIP RESTRAINTS NOT FEASIBLE**
-

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DEFENSE-IN-DEPTH

- **ALL CREDITED SAFETY SYSTEMS PROTECTED FROM PIPEWHIP**
 - **DESIGNED/CONSTRUCTED/INSPECTED TO NUCLEAR STANDARDS**
 - **INAUGURAL FLAWS WILL NOT GROW THROUGH THE WALL**
 - **TOLERANT TO READILY DETECTABLE THROUGHWALL (LEAKING) CRACKS**
-

MJK-IPRQ/89

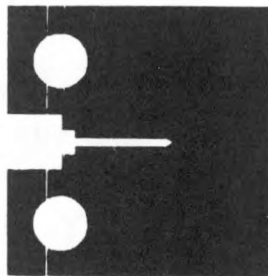


MATERIAL PROPERTIES

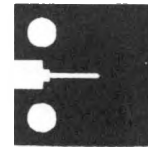
TENSILE



FRACTURE TOUGHNESS



FATIGUE CRACK GROWTH



MJK/88

FACTORS AFFECTING MATERIAL PROPERTIES

- **METALLURGICAL HEAT**
 - **MANUFACTURING**
 - **TEMPERATURE**
 - **STRAIN RATE**
 - **ORIENTATION**
 - **LOADING HISTORY**
 - **AGING**
-

MJK-IP192/88

PIPING LOADS

- **TAKEN FROM ASME CLASS 1
PIPING ANALYSES**
 - **ASME SERVICE TRANSIENTS
FATIGUE CRACK GROWTH OF
INAUGURAL FLAW**
 - **LBB REFERENCE TRANSIENTS
STABILITY OF:**
 - **END-OF-LIFE FLAW**
 - **LEAKING CRACK(S)**
-

MJK-IP192/88

LEAKAGE DETECTION

- **INVENTORY CHANGES**

STORAGE TANK LEVEL

- **LIQUID DETECTORS**

SUMP RECOVERY

COLLECTION TANK LEVEL

BETTLES

- **VAPOUR DETECTORS**

DRIER COLLECTION

EXHAUST STACK MONITOR

MJK-IP19Q/89

POSTULATED CRACK GEOMETRIES



SURFACE FLAW



LEAKING CRACK

MJK/88

TOPICS TO BE COVERED

BACKGROUND

LBB FOR CANDU PIPING

APPLICATION TO DARLINGTON

VERIFICATION OF TECHNOLOGY

MJK-IPRQ/88

APPLICATION TO PIPING

- ASME CLASS 1 PIPING
 - 21/22/24" NPS SCHEDULE 100
 - A106 GR.B CARBON STEEL
 - ALL WELDS ARE:
 - RADIOGRAPHED
 - 100% ULTRASONIC
 - PWHT
 - NO CORROSION
-

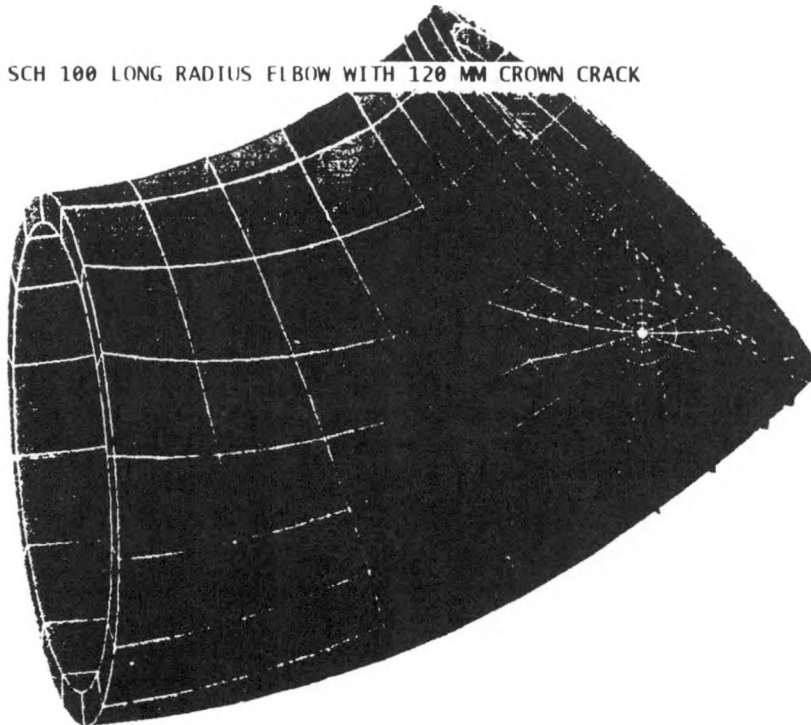
MJK-IPRQ/88

FINITE ELEMENT ANALYSES

- ABAQUS VERSION 4.7
 - DEFORMATION PLASTICITY
 - 20 NODED SOLIDS
REDUCED INTEGRATION
 - 1 LAYER THROUGH THICKNESS
 - COLLAPSED CRACK-TIP ELEMENTS
 - VIRTUAL CRACK EXTENSION
SELF-SIMILAR CRACK EXTENSION
 - STATIONARY CRACK
-

MJK-IPRQ/88

24 INCH SCH 100 LONG RADIUS ELBOW WITH 120 MM CROWN CRACK

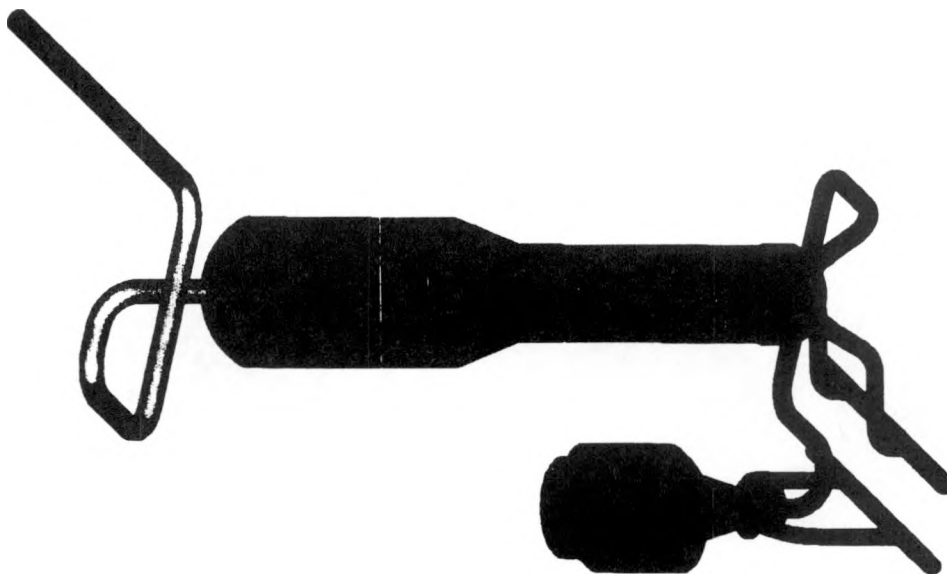


RESULTS TYPE? 1.STRESS MODEL 2.EXTERNAL DATA 3.MODEL FEATURES 4.END
>

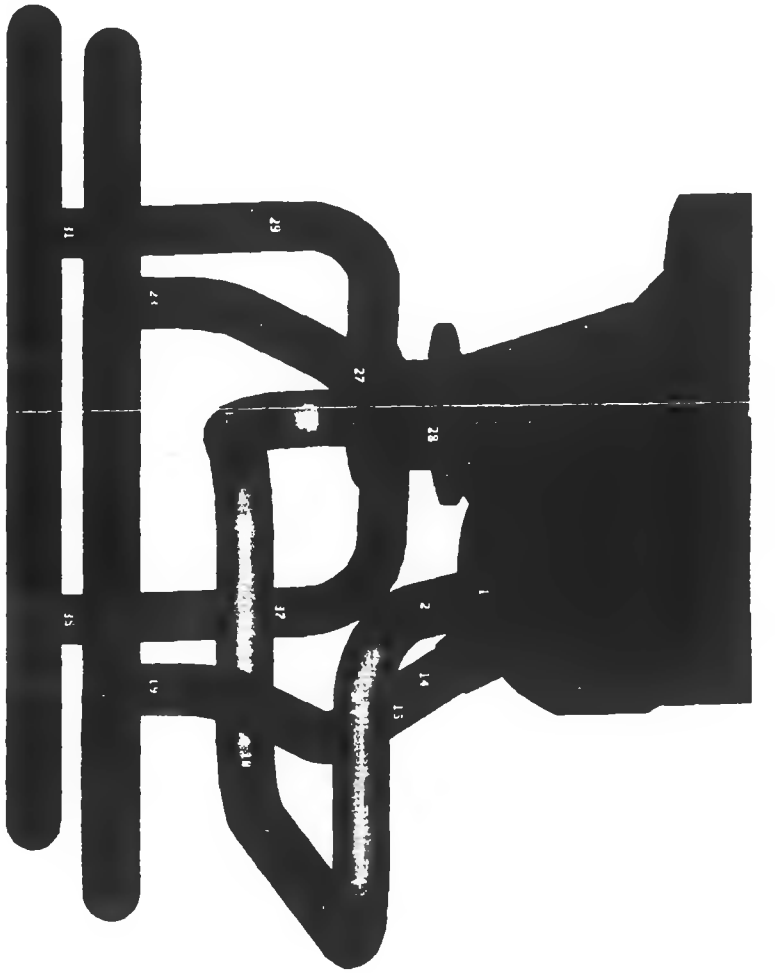
ASSESSMENT OF PIPING

- DETERMINE ALL LOADS
 - FATIGUE CRACK GROWTH OF POSTULATED FLAW
 - SIZE THROUGHWALL CRACK BASED ON LEAK DETECTION
 - DEMONSTRATE STABILITY OF:
 - END-OF-LIFE FLAW
 - LEAKING CRACK
-

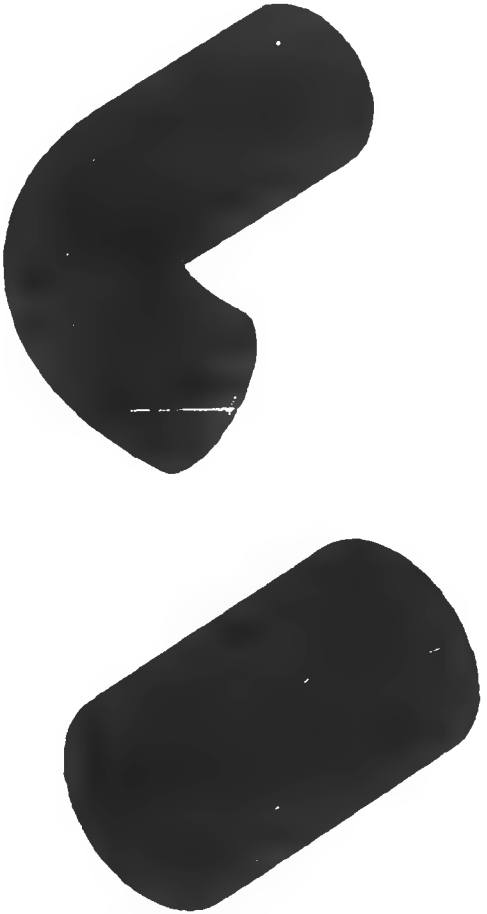
MLK-IPRO/88

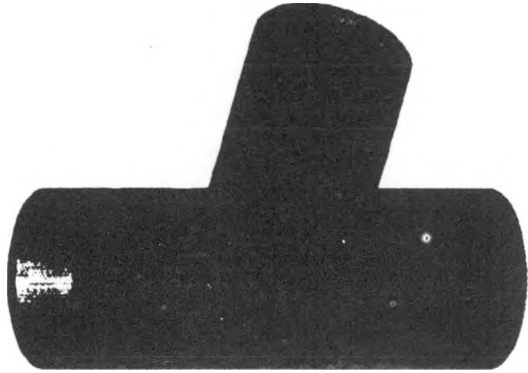


DARLINGTON NGS A

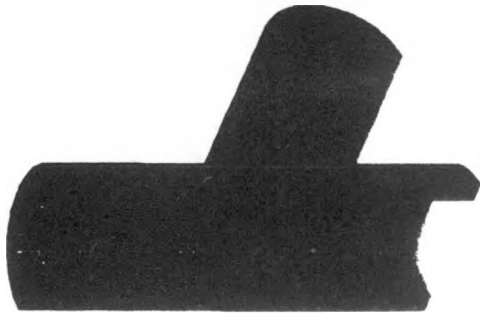


GIRTH-BUTT WELD
CIRCUMFERENTIAL CRACK

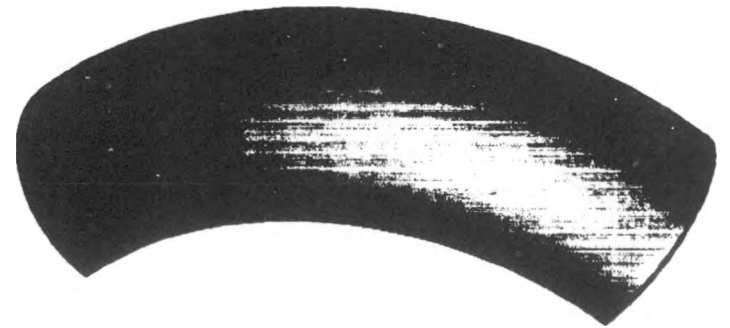


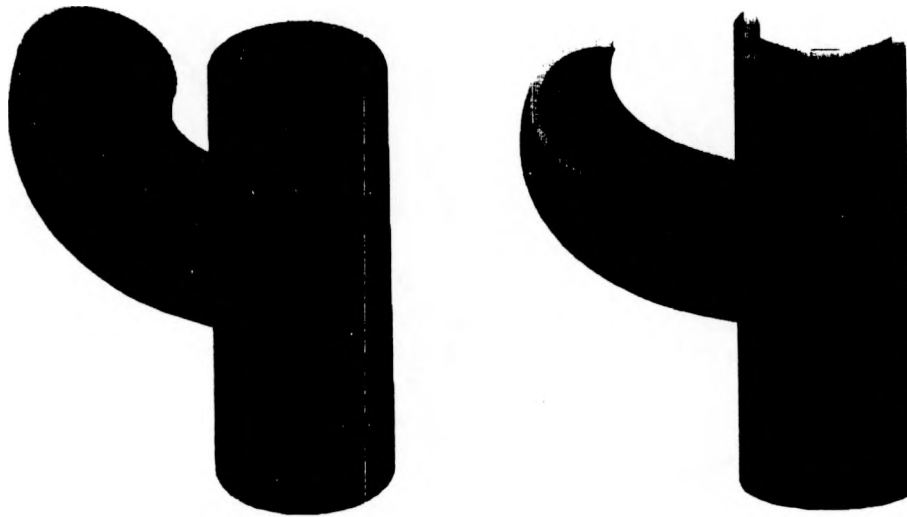


**RIH BRANCH CONNECTION
AXIAL CRACK**



**LONG RADIUS ELBOW
AXIAL CRACK**





**ROH BRANCH CONNECTION
AXIAL CRACK**

RESULTS OF ASSESSMENT

**LIMITING FATIGUE GROWTH
HEADER BRANCH CONNECTION**

**LIMITING CRACK STABILITY
BOILER INLET ELBOW**

**LIMITING LOADS
SEISMIC & MAXIMUM PRESSURE**

MJK-IP1RQ/86

TOPICS TO BE COVERED

BACKGROUND

LBB FOR CANDU PIPING

APPLICATION TO DARLINGTON

VERIFICATION OF TECHNOLOGY

MJK-IP1RQ/88

FEM MODELLING ISSUES

- VERIFY ABAQUS**
 - ELEMENT SELECTION**
 - MESH REFINEMENT**
 - CRACK-TIP ELEMENT MESH**
 - CONVERGENCE TOLERANCES**
 - MATERIAL MODELLING**
 - VERIFY MODEL**
-

MJK-IP1RQ/88

LEAKRATE VERIFICATION

- **BURST TEST FACILITY**
 - **TEST PARAMETERS:**
 - **CRACK LENGTH**
 - **COD**
 - **FLUID SUBCOOLING**
 - **MEASURE:**
 - **MASS FLUX**
 - **THROUGHWALL TEMP.**
 - **THROUGHWALL PRES.**
-

MJK-IPIRG/89

EPFM VERIFICATION

- **JOIN IPIRG**
 - **US-NRC DPP RESULTS**
 - **ONTARIO HYDRO**
 - **MATERIAL TESTING**
 - **BURST TEST FACILITY**
-

MJK-IPIRG/89

BENEFITS OF LBB

- **COST BENEFIT 15:1**
 - **REDUCED MANREM**
 - **MORE RATIONAL DESIGN**
 - **INCREASED LICENSABILITY**
-

MJK-IPRQ/88

REGULATORY EXPERIENCE IN CANADA
ON LEAK-BEFORE-BREAK

ABSTRACT

For the licensing of Ontario Hydro's Darlington reactors the staff of the Atomic Energy Control Board were prepared to consider proposals to omit restraints in certain circumstances. Each unit at Darlington has about 100 km of piping, most of which is small-diameter feeder piping. All but the largest (21") piping has been designed to meet the Board's requirements for protection against postulated pipe breaks. For the 21" diameter piping Ontario Hydro requested that they omit restraints and presented a case based on Leak-Before-Break to substantiate their request.

Meanwhile the designers at Atomic Energy of Canada Ltd. reviewed the pipe-rupture case for their CANDU-3 design and concluded that by separation and barriers there was no need to invoke Leak-Before-Break arguments for large diameter piping. However, the CANDU-3 design is approximately half the size of Darlington, there is less piping and the largest pipes are smaller diameter.

One important difference between the CANDU-3 and Darlington reactors is the design of the containment. The CANDU-3 containment is a conventional positive pressure design and the concrete envelope is remote from the primary heat transport piping. As a consequence the concrete support structures and barriers put in place to mitigate the potential damage from jets and whipping pipes do not form part of a pressure boundary. This is not the case for Darlington, whose containment design is linked to a vacuum building, allowing the containment envelope to be very close to the reactor and piping. In this case the concrete must sustain impact damage without through-wall cracking.

A review of the licensee's case for Darlington led AECB staff to conclude that the licensee had not demonstrated that piping failures, including rupture, were sufficiently improbable that their consequence need not be considered. It was felt that it was desirable to limit the consequences of a rupture by separating equipment, incorporating barriers, and where practicable, restrain piping. This resulted in a hybrid approach which reflected the belief that ruptures of large-diameter class 1 pipes were infrequent; but because this was not well proven some protection should be maintained.

Ontario Hydro's study of the feasibility of the piping restraints and their break consequence analysis identified a number of grey areas where the analyses were judged speculative and the predicted outcome marginal. For these, barriers or restraints were required.

It is implicit in this line of reasoning that the need for protection is not being challenged, only the degree or manner in which it is given. With this view the Leak-Before-Break option has to include factors which could reduce the potential for failure of the piping.

Restraints are a passive long-term defence against rupture. The leak-before-break option must also include elements which are long-term.

For instance, one approach could be to examine the past history of failures in similar pipes and put controls in place to reduce their frequency. An exercise like this has been done by Ontario Hydro, although their intent was to show that their 21" piping was unique to and better than the data base from which our failure statistics were drawn.

To date there is insufficient data to perform this exercise in anything other than a qualitative manner. However, such an approach in itself implies a belief that the rupture rate of 21" diameter piping is very low, less than 1 in a million per reactor year. It also suggests that by reducing the frequency of leaks the frequency of ruptures will be lowered.

If we examine the statistics for leaks and ruptures in CANDU reactors we find that there are no large-diameter class 1 ruptures in the data base. However, statistical inference from data for smaller-diameter piping typically gives values around 10^{-5} for failures/per reactor year. Failures in this context are not limited to double-ended guillotine breaks, and do not necessarily include breaks for which pipe whip analysis would predict an unacceptable outcome.

The Ontario Hydro compilation of failure incidents was done from reports which are now five years old. If conditions have changed from that date, a review of the failure statistics from that time should indicate an improving trend. A cursory review of our significant event data base suggests that the failure rates are similar. It is evident in all the failure statistics analyzed to date that most of the reported failures are associated with failure modes and/or equipment which do not directly relate to our conception of what the precursor to a pipe severance should look like. It is also apparent in this review that failures on small-diameter equipment predominate, and that effective controls can be put in place to eliminate all of them. The same controls will be equally effective on the large-diameter PHT piping.

For example, the Periodic Inspection Program could be arranged to address issues such as erosion/corrosion and "harmonic" fatigue in those pipes for which LBB is credited. Similarly, controls on water chemistry could be mandated. A recent AECB study estimates that the secondary-side water chemistry is out-of-specification 20% of the time, and the primary 5%. Post-weld heat treatment should be mandatory for pipe joints and attachments, and completion and commissioning QA focussed on procedures deemed to be of leak-before-break significance such as injection tests for water hammer, restraint clearances, and vibration monitoring.

These measures should change the failure rates of the 21" diameter piping. If we assume this is currently around 10^{-3} events/year, perhaps 1 in 10 of these could be a double-ended break, and we need about 10^{-2} extra comfort from the controls discussed above and/or from the limited application. The unrestrained 21" piping represents about 6% of the total piping used in the 10^{-3} failure estimate. Combining this with the rupture and failure-rate estimates above gives an overall value between 10^{-5} and 10^{-6} .

Our perception of this estimate is that it implies things which are outside the design constraints on the 21" piping. This is where we look to the IPIRG program to tidy up some loose ends:

- To demonstrate that ruptures of large-diameter PHT piping requires degradation and/or loading conditions which are reasonably outside the design constraints; and
- to demonstrate that the portion of ruptures in the data base can be eliminated by timely response to leaks.

The first of these adds nothing to safety. The second, however, can be used effectively to specify a leak-detection system.

Summarizing all the above: To accept leak-before-break in lieu of piping restraints the AECB require that:

1. the failure rate of the piping under consideration is expected to be acceptably low;
2. leak detection systems and response procedures are in place which can react to small leaks;
3. if necessary to ensure 1, demonstrate margins to safety with leak-before-break analysis, and where required implement special measures to monitor for known degradation mechanisms and their causes.



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REGULATORY EXPERIENCE IN CANADA OF LEAK-BEFORE-BREAK

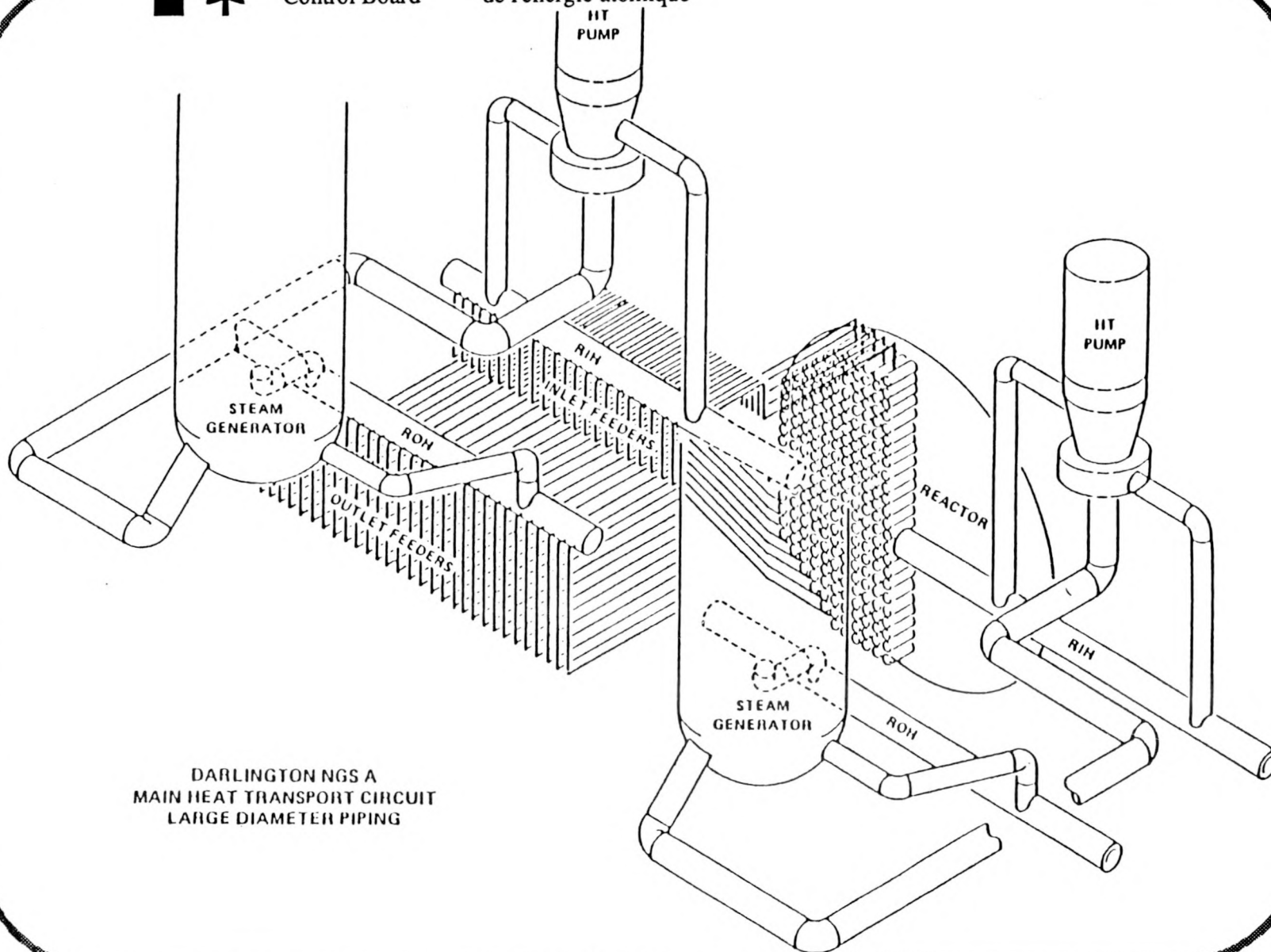
**FOCUSES ON: ONTARIO HYDRO'S
DARLINGTON NGS**

**AND ATOMIC ENERGY OF CANADA LIMITED
CANDU-3**



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Control Board

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DARLINGTON NGS A
MAIN HEAT TRANSPORT CIRCUIT
LARGE DIAMETER PIPING



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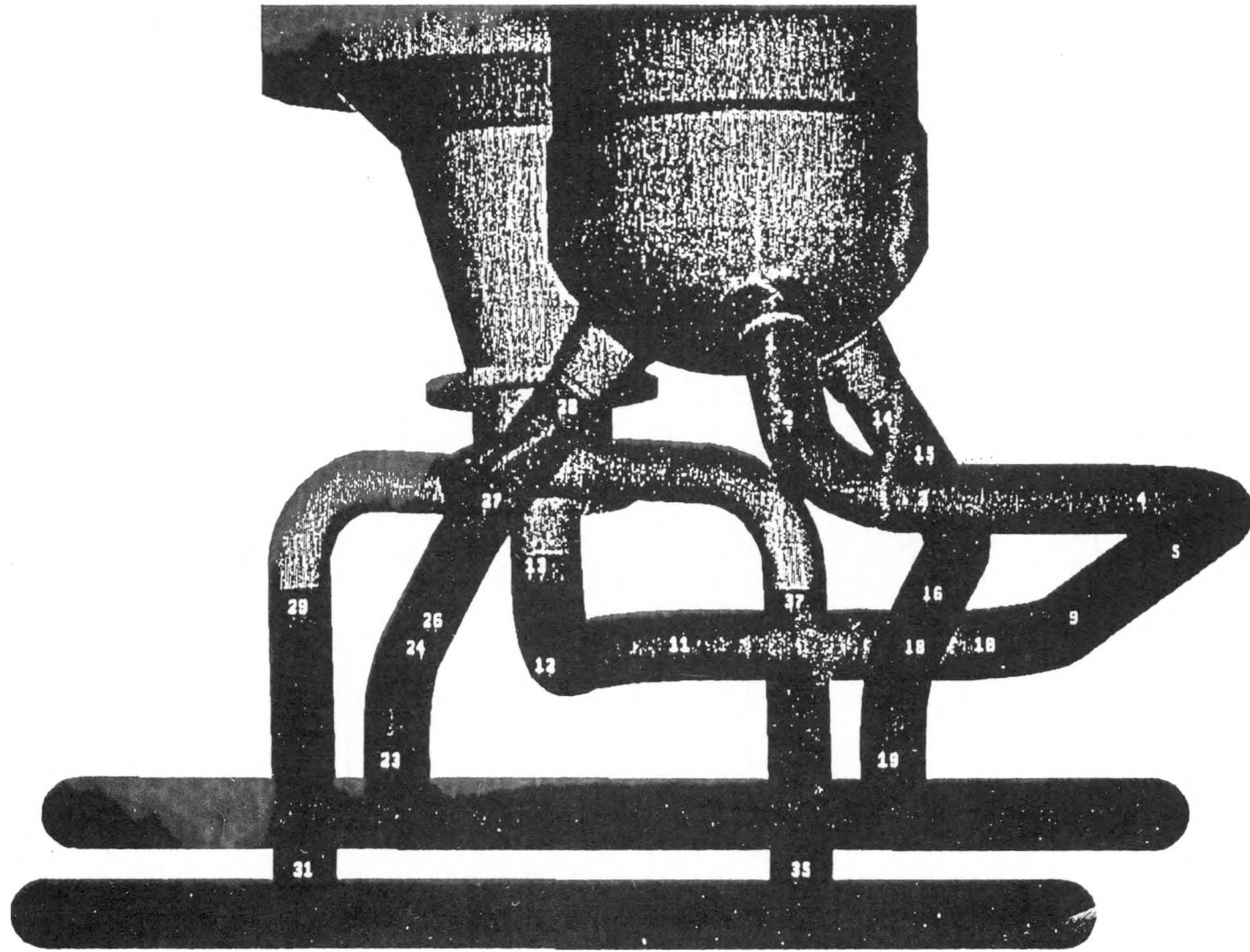
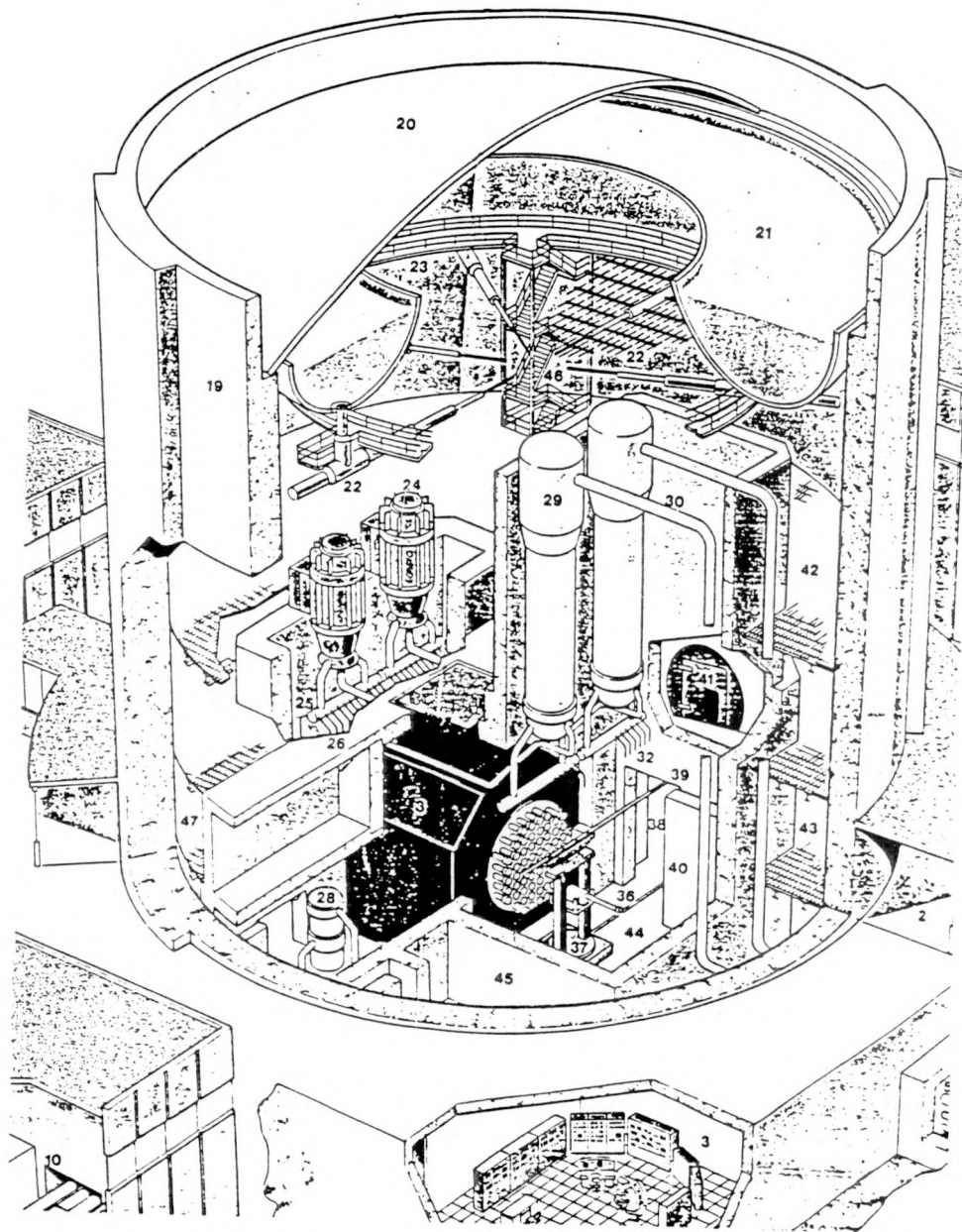


Figure 5: Schematic of Large Diameter Heat Transport System Piping (showing locations of the postulated cracks)

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CANDU 300



AECB REQUIREMENTS FOR A LEAK-BEFORE-BREAK APPROACH IN LIEU OF PIPING RESTRAINTS

MAINTAIN:

- SEPARATION OF EQUIPMENT
- BARRIERS
- CONSEQUENCE ANALYSIS

PLUS:

- REASONS WHY PROTECTION IMPRACTICAL; FRACTURE ANALYSIS; LEAK DETECTION SYSTEM

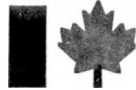


LBB ANALYSIS NOT USED FOR:

BREAKS WHICH COULD BREACH CONTAINMENT

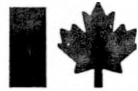
**BREAKS WHICH COULD DAMAGE SHUT-DOWN
SYSTEMS**

**BREAKS WHOSE CONSEQUENCE WOULD FURTHER
DISRUPT CORE COOLING**



ISSUES ARISING FROM DARLINGTON CONSEQUENCE OF RUPTURE ANALYSIS

1. REACTIONS FROM PHT PIPE BREAKS TEAR FEEDERS
2. REACTIONS FROM PIPE BREAKS BEND FEEDERS AND BLOCK CHANNEL FLOW
3. BOILER AND PUMP PENETRATIONS COULD BE DAMAGED SO THAT CONTAINMENT INTEGRITY IS THREATENED
4. HEADER BREAKS COULD PUNCH HOLES IN CONTAINMENT
5. BREAKS NEAR HEADERS COULD DAMAGE SDS-2
6. JETS OR PIPE WHIP COULD DAMAGE THE LOOP INTERCONNECT LINES



REGULATORY POSITION

- 1. NO DEFINITIVE INFORMATION THAT RUPTURES ARE SO INFREQUENT THAT THEY CAN BE IGNORED**
- 2. REPLACE CURRENT REQUIREMENTS FOR RESTRAINTS WITH SOMETHING COMPARABLE**



PERCEIVED MERITS/ DEMERITS OF LBB RESTRAINTS

RESTRAINTS:

- 1. GIVE PASSIVE, ON-GOING PROTECTION**
- 2. PROBLEMS FROM: ADDED COMPLEXITY/
INTERFERENCE AND EXPENSIVE**
- 3. REGULATORY ENFORCEMENT - LIMITED I.S.I.**

LEAK-BEFORE-BREAK:

- 1. ONLY A HYPOTHETICAL CONCEPT**
- 2. APPLICATION IS COMPLEX**
- 3. NO ON-GOING ASSURANCE**
- 4. APPLICATION ACTIVE**
- 5. REGULATORY ENFORCEMENT COMPLEX**

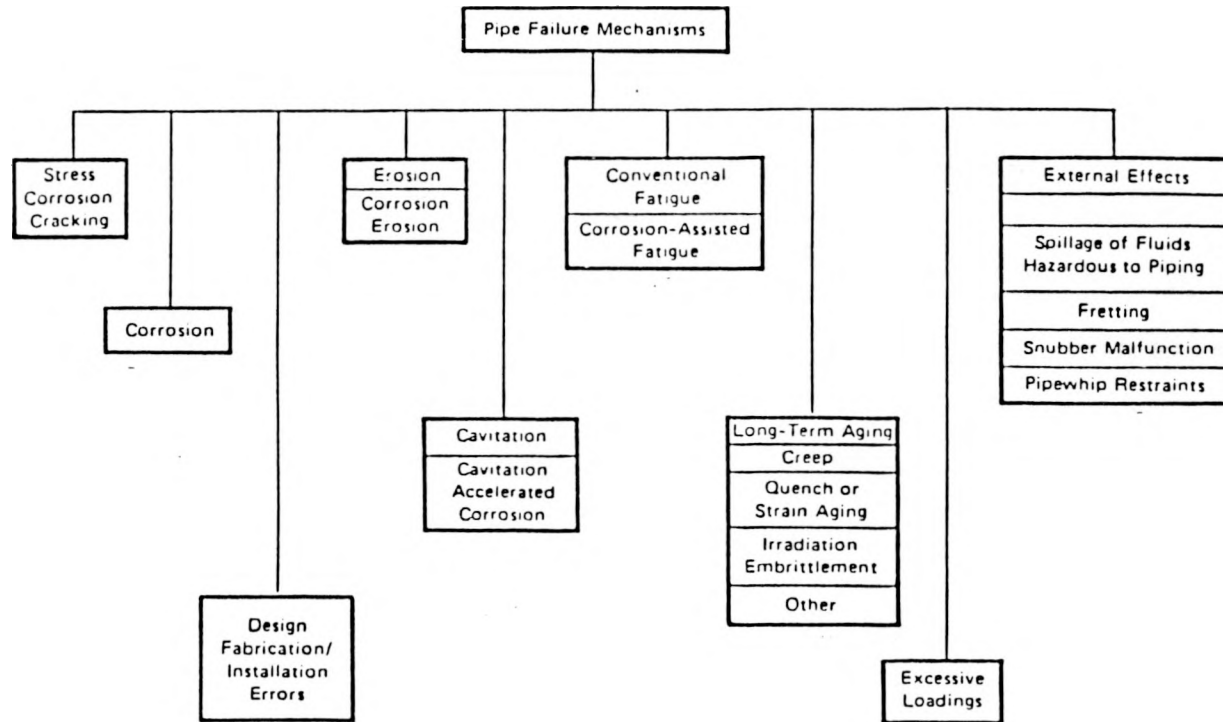


ESTIMATES OF PHT SEVERANCE RATES
BASED ON CANDU PERFORMANCE

Units	Size Range		
	Large (d ≥ 150 mm)	Intermediate (25 ≤ d < 150 mm)	Small (d ≤ 25 mm)
Number of Post-Criticality Severances in Total Plant Admissible in Alternative Estimate. λ_a^P	0	1	62
Average Nuclear Piping per plant, A	2800	16,300	12,300
Proportion per Category: nuclear to total plant, ν	0.18	0.39	0.55
Severance Rate, $\lambda_a^P = \nu \times \lambda_a^T$	$\lambda_a^P(IP)_{95} \leq 1.0 \times 10^{-2}$ $\lambda_a^P(IP)_{50} \leq 2.3 \times 10^{-3}$	$3.8 \times 10^{-4} \lambda_a^P(IP)_{90} \leq 3.5 \times 10^{-2}$ $\lambda_a^P(IP)_{90} \leq 1.2 \times 10^{-2}$	$5.1 \times 10^{-1} \lambda_a^P(SP)_{90} \leq 7.9 \times 10^{-1}$ $\lambda_a^P(SP)_{50} \leq 6.5 \times 10^{-1}$

Pipe Rupture Size (mm)	LOCA Initiating Rupture Rate per Plant per Year	
	90% Range	Median
12-50	1×10^{-4} to 1×10^{-2}	1×10^{-3}
50-150	3×10^{-5} to 3×10^{-3}	3×10^{-4}
>150	1×10^{-5} to 1×10^{-3}	1×10^{-4}

US PIPE FAILURE ASSESSED VALUES

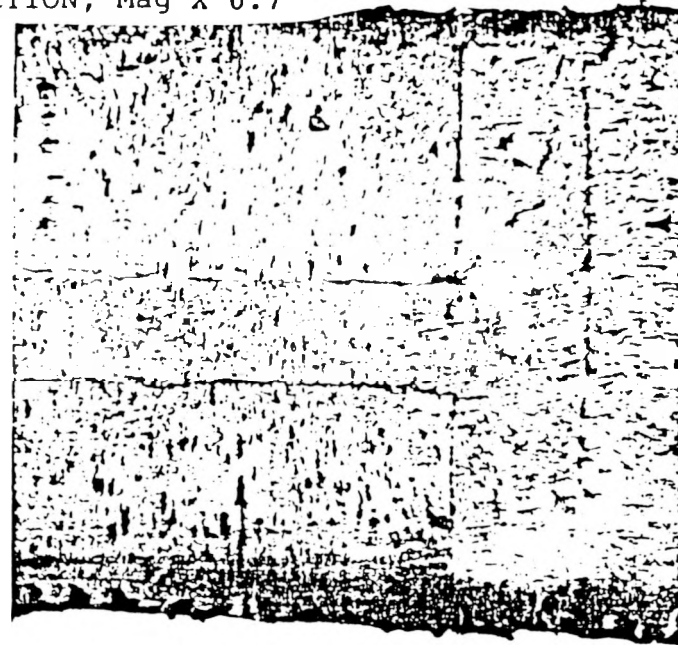
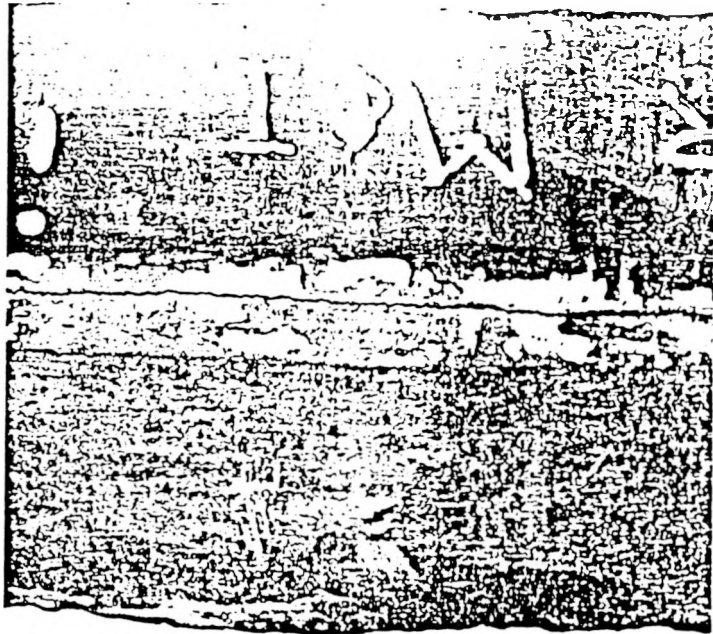


Assessments of Failure Mechanisms for Large Diameter
Heat Transport Piping for Darlington NGS A



FIGURE 2 - APPEARANCE OF INSIDE SURFACES AFTER DYE PENETRANT INSPECTION, Mag X 0.7

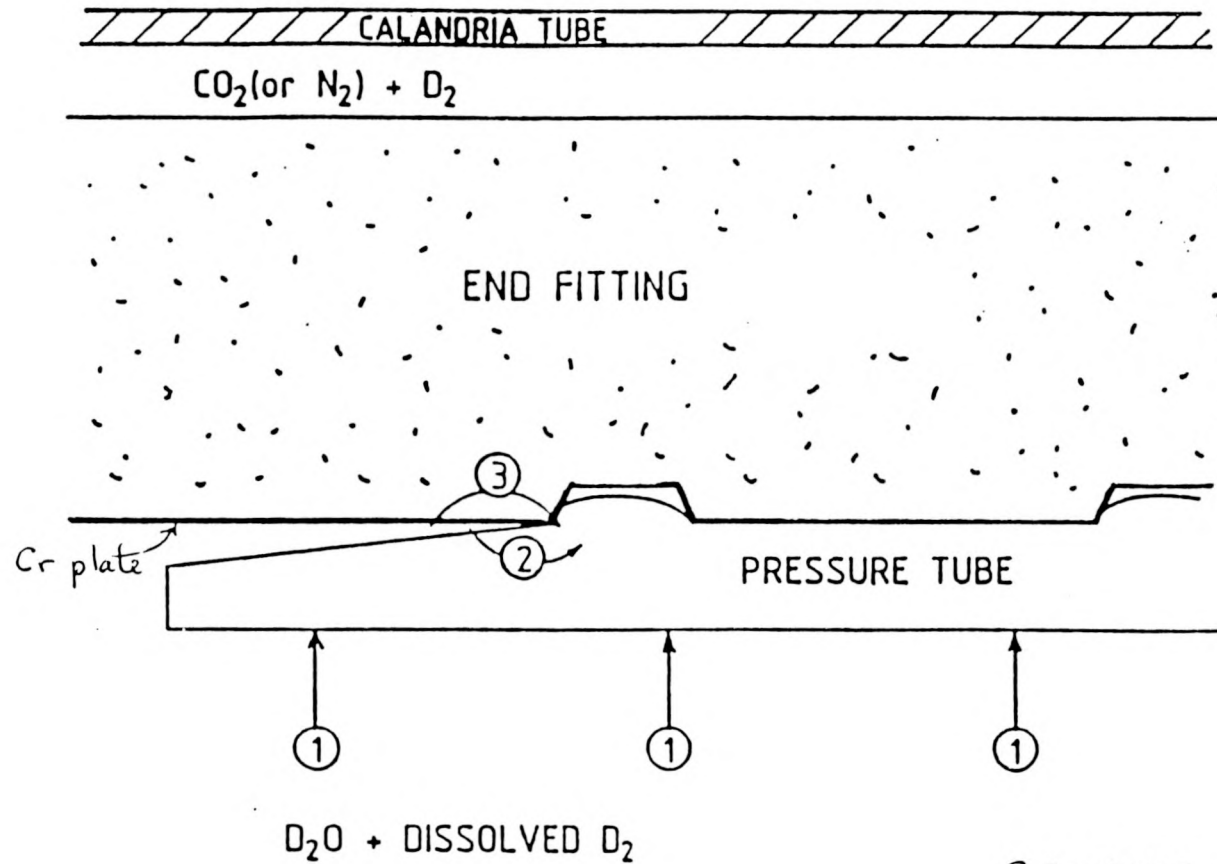
STRESS CORROSION CRACKING
BHWP - STAINLESS HEAT EXCHANGERS



b) Longitudinal and
Circumferential Welds

b) Outside Surface

FIGURE 1 - VISUAL APPEARANCE, CRACK SITE, HX 108-3, Mag X 0.7
CRACK LENGTH 350mm IN 12m x 0.6m VESSEL



1. D₂ UPTAKE FROM CORROSION
2. D₂ UPTAKE VIA THE OUTBOARD CREVICE
3. D₂ UPTAKE VIA THE END FITTING

GALVANIC CORROSION
END FITTING TO
PRESSURE TUBE



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ISN:3077 P6, B85-238
PICKERING-B, P6

DATE OF EVENT: 85-11-06
REPORTING PERIOD: 85, NOV-DEC
DATE OF INPUT: 1986-01-09 -DC

CAUSES-AECB: 1154
1122

CAUSES-UTILITY: 1224

SYSTEMS AFFECTED: S&FH

USI NUMBER: 36000
43210

EFFECTS: 2231

IMPAIR./FAULT: 3329

FOLLOW-UP: 4121

* ABSTRACT * BOILER FEEDWATER
SAMPLES INDICATED IRON & COPPER
CONCENTRATIONS 10 TIMES HIGHER
THAN EXPECTED DURING STARTUP. THIS
CORROSION WAS DUE TO LACK OF
HYDRAZINE ADDITION DURING THE
STARTUPS.

* FOLLOW-UP REMARKS * 1. ENSURE
CORRECTNESS OF CHEMICAL SPECS. 2. ENSURE
ADEQUATE CONTROL OF CHEMICAL SAMPLING.

HED, APR 19, 1989

2

ISN:3896 G2, 85-622
GENTILLY, G2

DATE OF EVENT: 85-06-01
REPORTING PERIOD: 86, APR
DATE OF INPUT: 1986-05-07 -DC

CAUSES-AECB: 1127
1121

CAUSES-UTILITY:

SYSTEMS AFFECTED: ECCS

USI NUMBER: 34320

EFFECTS: 2955
2922
2236
2287

IMPAIR./FAULT: 3219
3719

FOLLOW-UP: 4320

* ABSTRACT * DURING THE SECOND 85
QUARTER, 4 TYPE 5 ECC FAULTS WERE
REPORTED: CORROSION CAUSED A LEAK
ON THE RECIRCULATION LINE OF HPECC
PUMP 34320-P3 (TWICE) AND DURING
TESTING PUMPS P1 & P2 TRIPPED
SPURIOUSLY.

* FOLLOW-UP REMARKS *



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ISN:2300 L1,83-708
POINT-LEPREAU,L1

DATE OF EVENT:83-07-15
REPORTING PERIOD:83,SEP
DATE OF INPUT:1985-06-04 -KH

CAUSES-AECB: 1121
1156

CAUSES-UTILITY:

SYSTEMS AFFECTED: S&FW

USI NUMBER: 43230

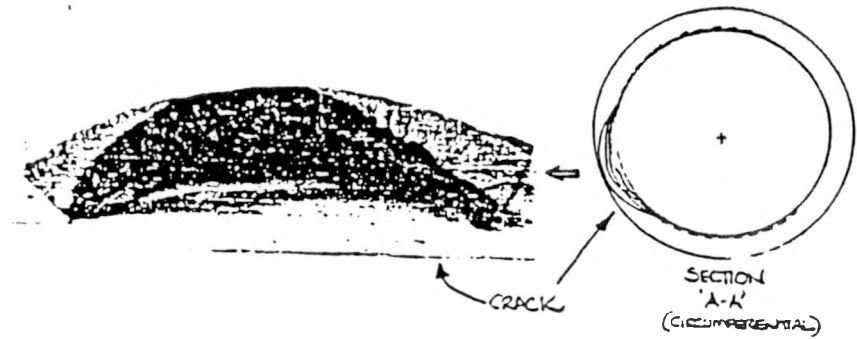
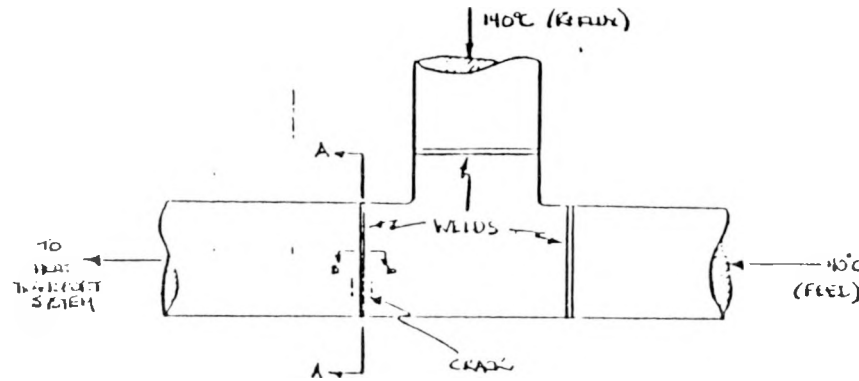
EFFECTS: 2231
2281

IMPAIR./FAULT: 3129

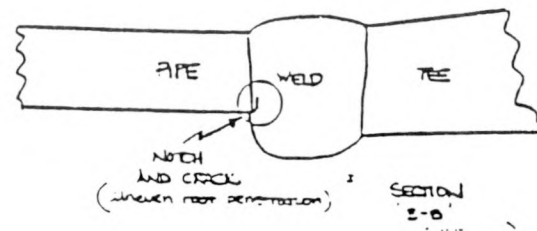
FOLLOW-UP: 4221

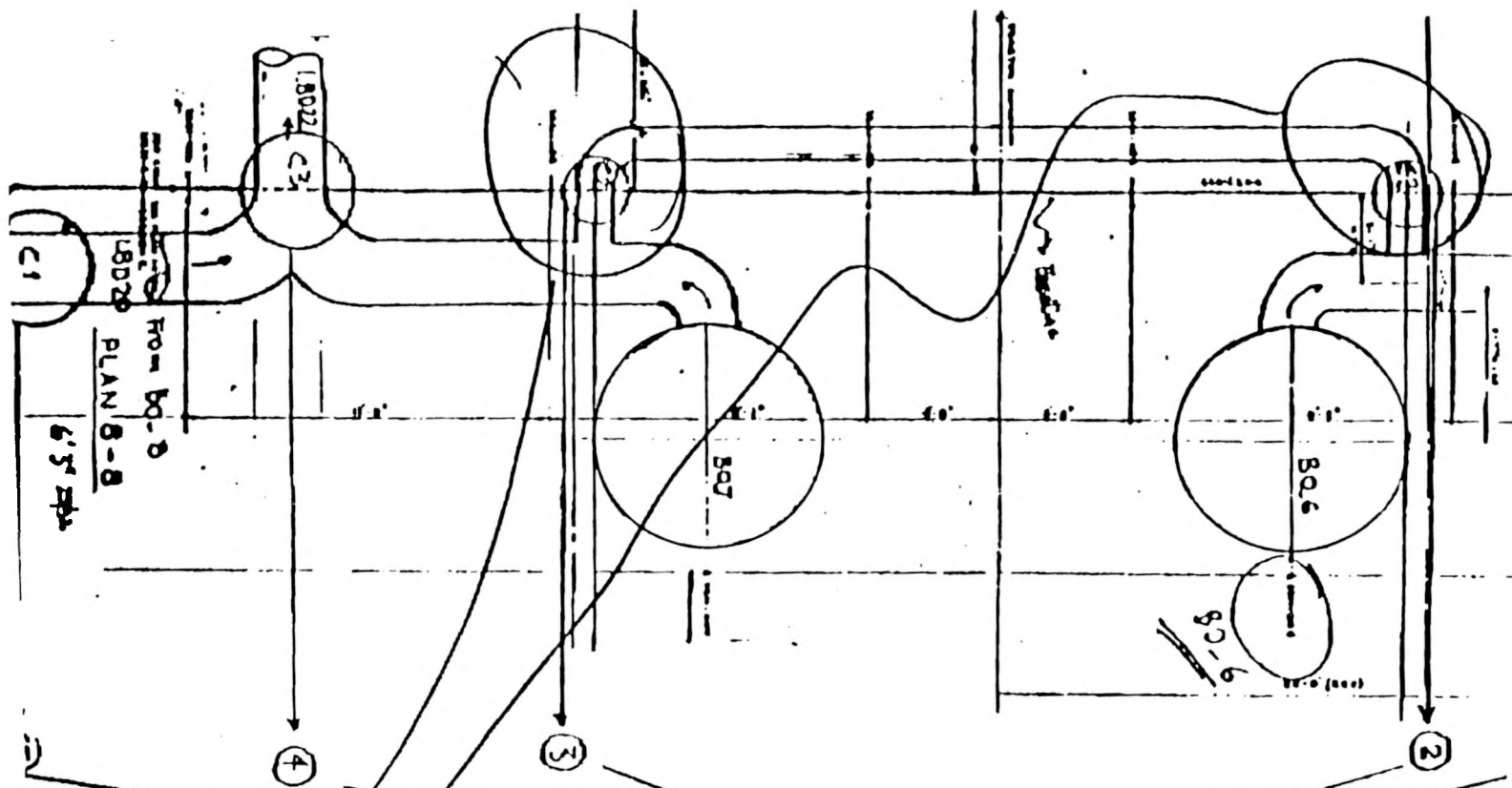
* ABSTRACT *A BOILER FEED PUMP
FAILURE AT G2 PROMPTED THE
INSPECTION OF PUMP 103 ROTATING
ASSEMBLY. (CAVITATION DAMAGE) WAS
FOUND, AND A PIECE OF MISSING VANE
METAL WAS FOUND. MISALIGNMENT WAS
CORRECTED. OTHER BFP'S WERE
INSPECTED. (BFP DAMAGE,
43230-P101/102/103).

* FOLLOW-UP REMARKS *



FATIGUE FAILURE OF
MIXING TEE @ BRUCE NGS 'A'
S-S FEED/BLEED CIRCUIT



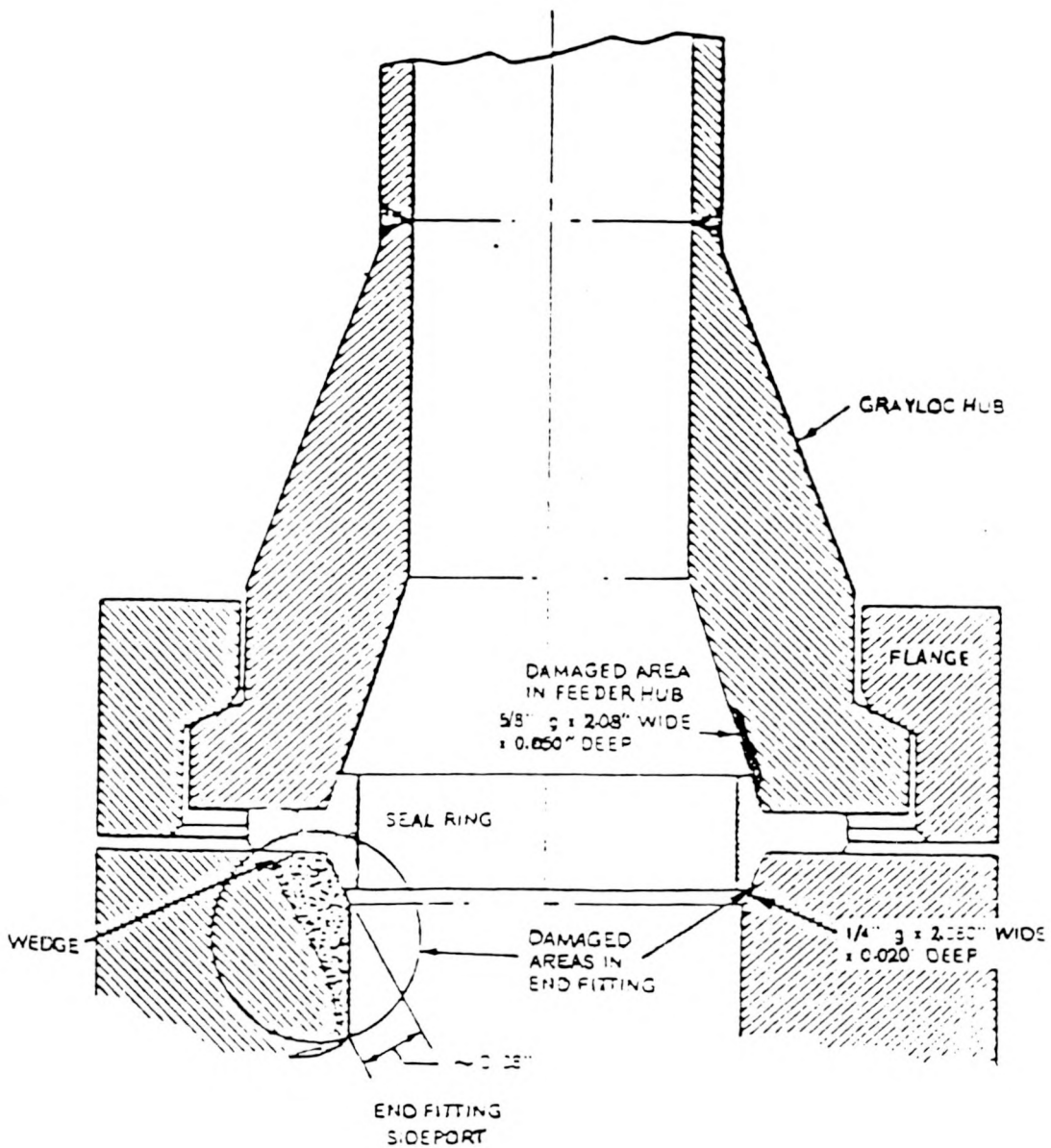


FRET LOCATIONS
ON FEED LINE.

6" long 80% thro' wall

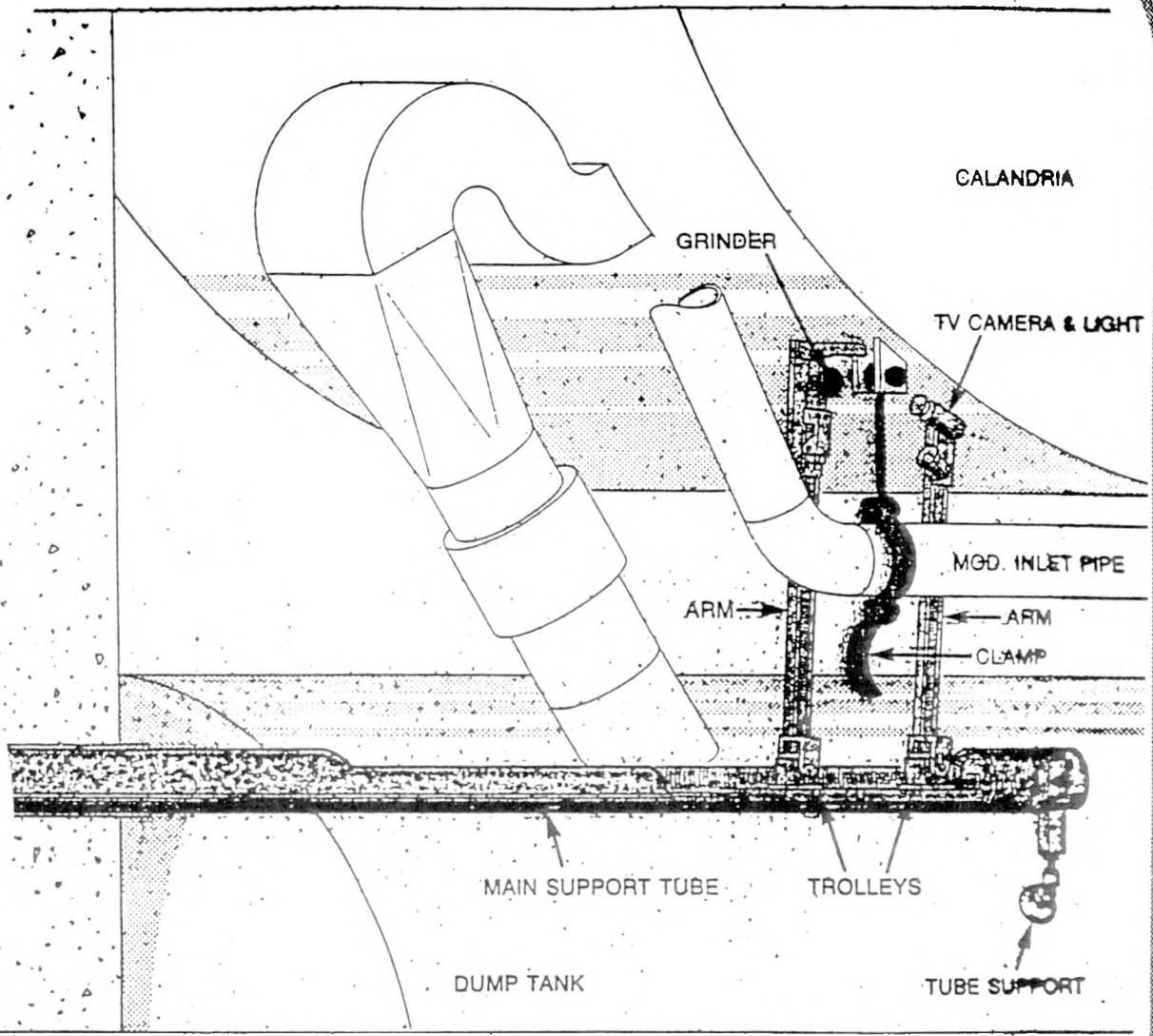
Check for interference & fretting of both the feedline and associated line
 Record clearance in between the piping

BRUCE NGS 'A'
 FEEDLINE
 FRETTING



CHANNEL J15 WEST, FEEDER CONNECTION REGION
SHOWING DAMAGE TO COMPONENTS

PICKERING 'A' END FITTING FRETTING



MODERATOR INLET PIPE — PIPE HANGER REPLACEMENT



PICKERING A PIPE HANGER FAILURE
RESULTING IN MODERATOR
INLET PIPE VIBRATION

the linear motion of the telescoping cylinder
 the design limits of the arrestor.

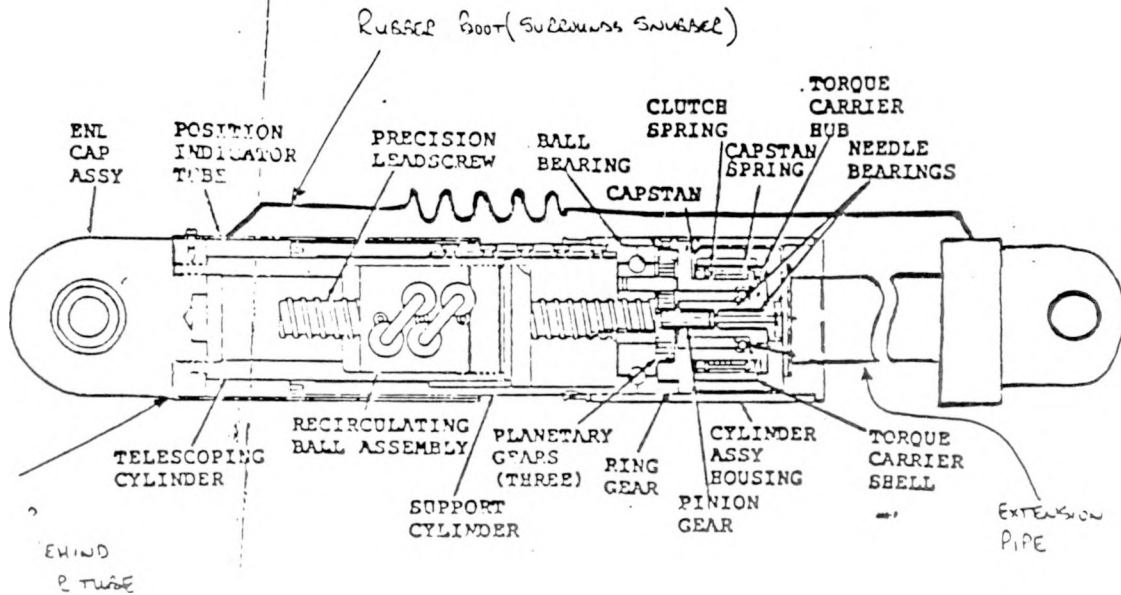


Figure 1-2. Arrestor Cross-Sectional View

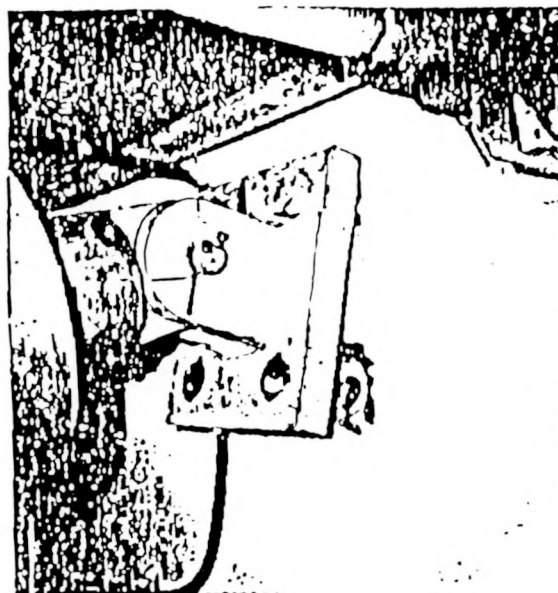
capstan spring will apply braking action in extension loading because the resisting mechanism is completely symmetrical in design. The moment drops below the threshold value it

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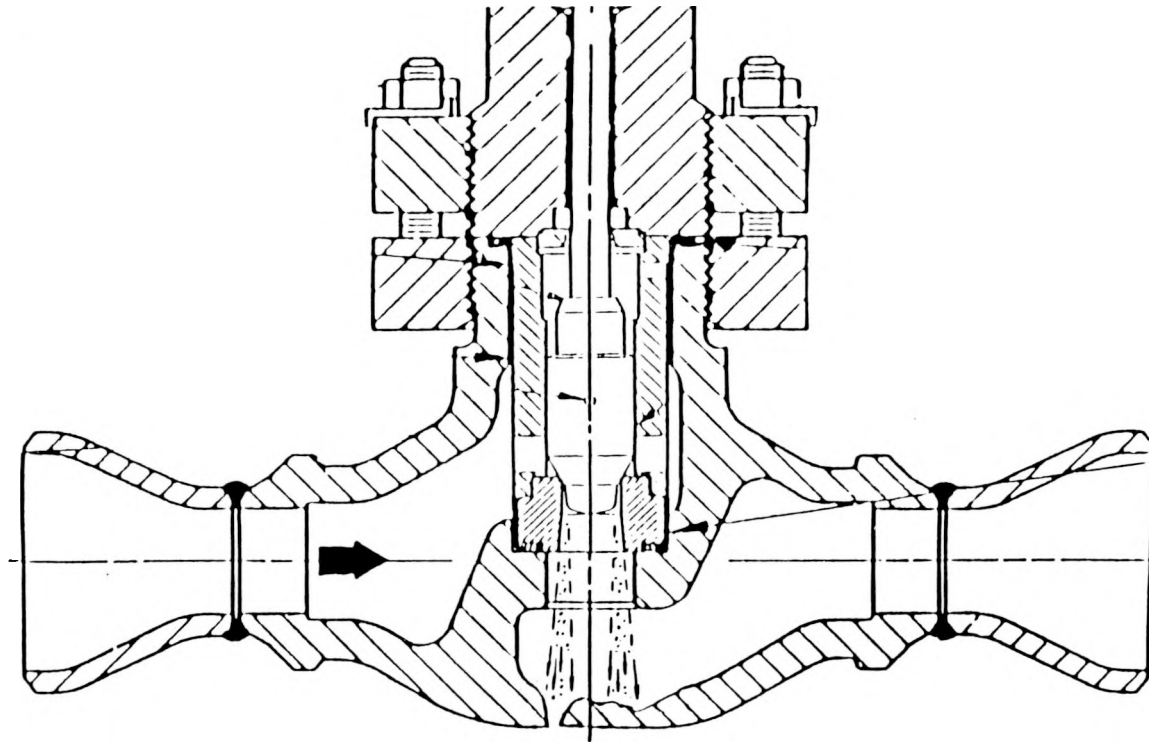
Atomic Energy
 Control Board



SNUBBER FAILURES - 45 AT PNT LEPREAU
 - 1 AT BRUCE B.



FAILED SNUBBER WITH REAC. PLATE DETACHED
FROM CONTAINMENT WALL.
BNGS #6 MARCH 1985
NW PHT QUAD.

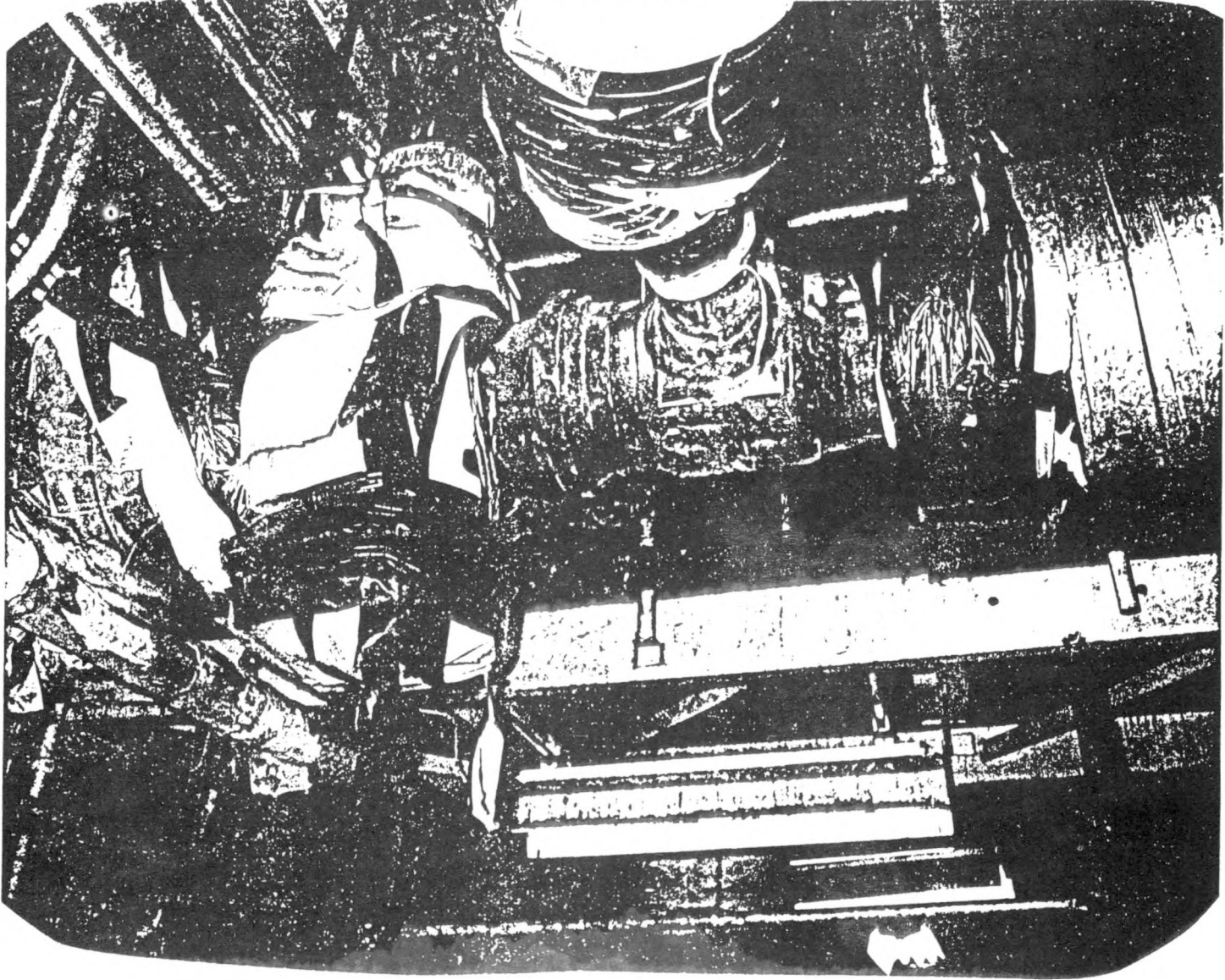


BRUCE 'B' - EROSION OF FEED/BLEED VALVES
DAMAGED AREA $\approx 2\frac{1}{2}$ "



Atomic Energy
Control Board

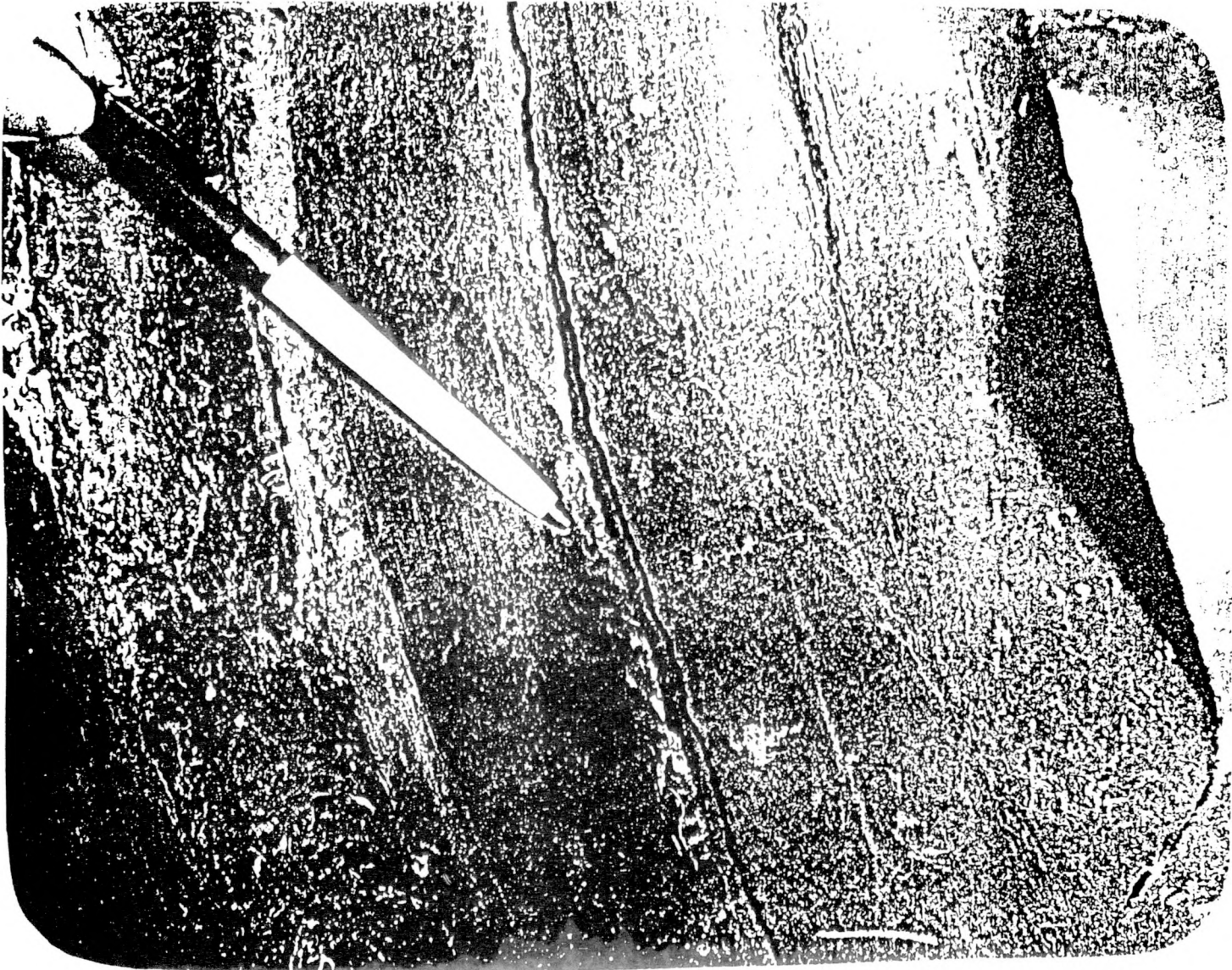
Commission de contrôle
de l'énergie atomique





Atomic Energy
Control Board

Commission de contrôle
de l'énergie atomique





ESTIMATES OF BBL

FROM - A.MITCHEL - RESEARCH AND
PRODUCTIVITY COUNCIL

POWER PIPING .13 } FROM RODABAUGH
REACTOR PIPING .1 }

SMALL DIA REACTOR PIPING .1 } JANTZEN
LARGE DIA REACTOR PIPING .05 }



LBB CONCEPT APPLIED TO DARLINGTON

1. IMPLIES CHANCE OF RUPTURE OF C/S PHT

-6

PIPING - (18 ins. DIA OR GREATER) LESS THAN 10
EVENTS / REACTOR YEAR

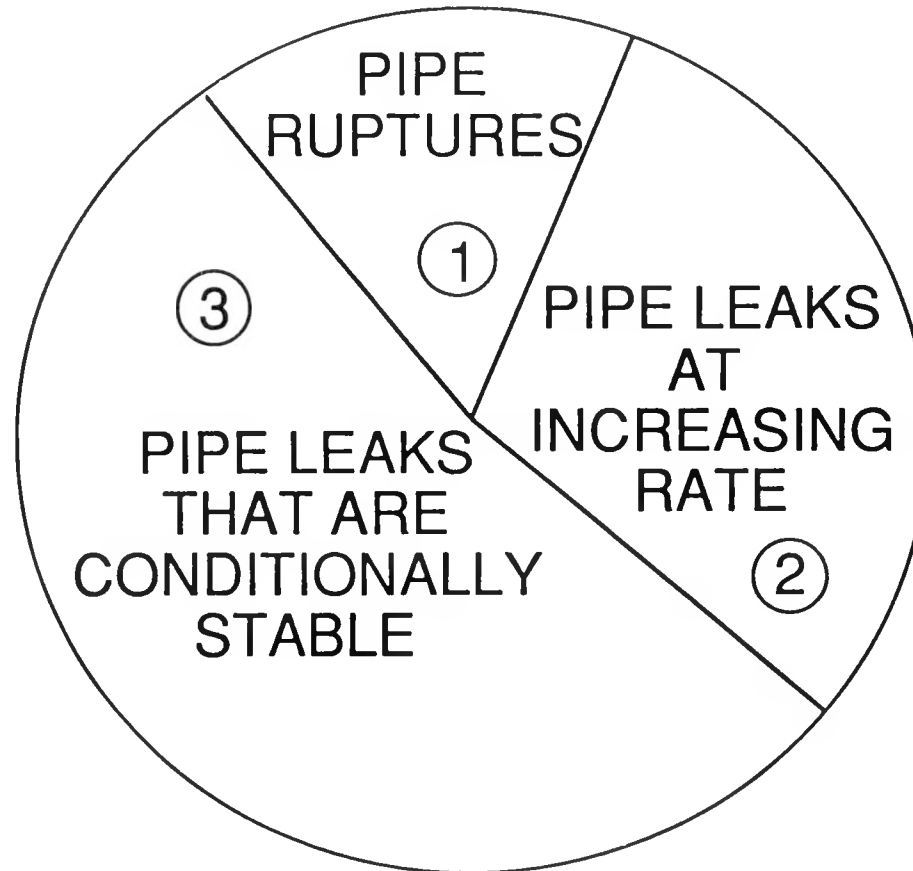
2. STATISTICS OF FAILURE BASED ON EXTRAPOL-
ATION OF SMALL DIAMETER - FOSSIL AND
SECONDARY SIDE "BIASED" DATA. CAN BE USED AS
AN INDICATOR FOR 1 ABOVE.

3. A TIMELY RESPONSE TO LEAKS COULD FURTHER
REDUCE THE RUPTURE STATISTICS IN 2 TOWARDS 1

4. THE RESPONSE TO LEAKS REQUIRED TO MAKE 3
PRACTICAL - ARE TOLERABLE IN TERMS OF REACTOR
PERFORMANCE



LEAK-BEFORE-BREAK CONCEPTUAL SCHEME INCLUDES



PERCEPTION FROM FAILURE DATA THAT $3 > 2 \gg 1$

AECB/UNIVERSITY OF TORONTO REVIEW OF CRACK PROPAGATION IN CARBON STEEL

- 1. J-R curves have a minimum value between 130°C and 250°C**
- 2. J-R curves for L-C and C-L orientation are significantly different**
- 3. The difference between L-C and C-L is a minimum at their minimum values**
- 4. Side-grooved data is more conservative than non-side-grooved**
- 5. No minimum-thickness side-grooved specimen can be deemed to provide a conservative J-R curve**
- 6. Orientation of stress-strain data not important**
- 7. Range of Ramberg-Osgood fit important**





"I am concerned that the leak-before-break may become a rationale for eliminating important and effective measures in the defense in depth concept (i.e., inservice inspection) along with poorly conceived measures such as pipe whip restraints. I am also concerned with confusion with LBB and low probability of failure. One should require both LBB and low probability of failure before policy changes are made. LBB provides a defense in depth measure, since the occurrence of leaks will provide a warning and opportunity to implement corrective measures to the elimination of leaks as well as potential breaks. Another concern is that LBB will lead to "overly fancy" criteria and analysis schemes, that are based on speculation and assumptions about crack length, leak rates and leak detection. It appears that LBB requires all of the following: high toughness, low stresses, and short cracks. One must have assurance that each requirement is satisfied, and not place too much focus on any one of the requirements."

**PIPE FRACTURE BEHAVIOUR RESEARCH
PROGRAMS IN ITALY**

Pietro P. Milella

IPIRG-TAG Meeting

Taipei

May 10, 1989

N E D B P R O G R A M

DURATION: 1981 - 1987

OBJECTIVE:

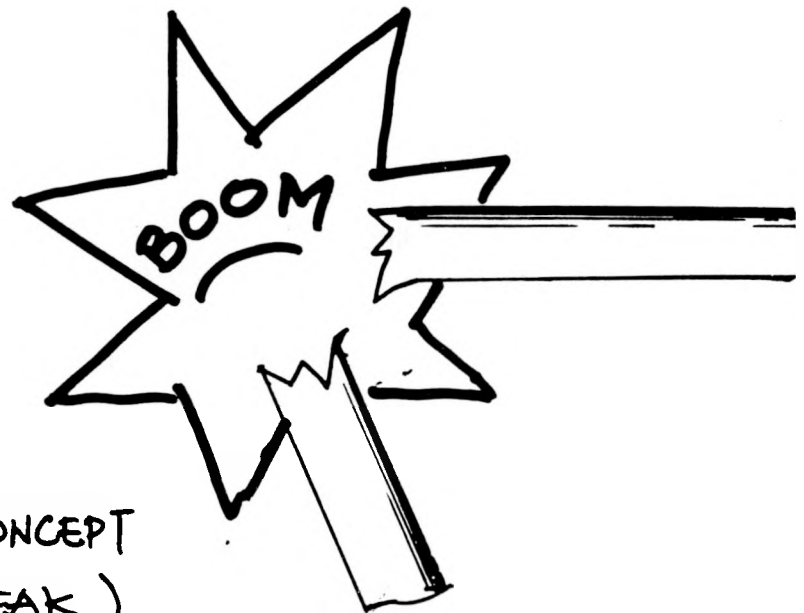
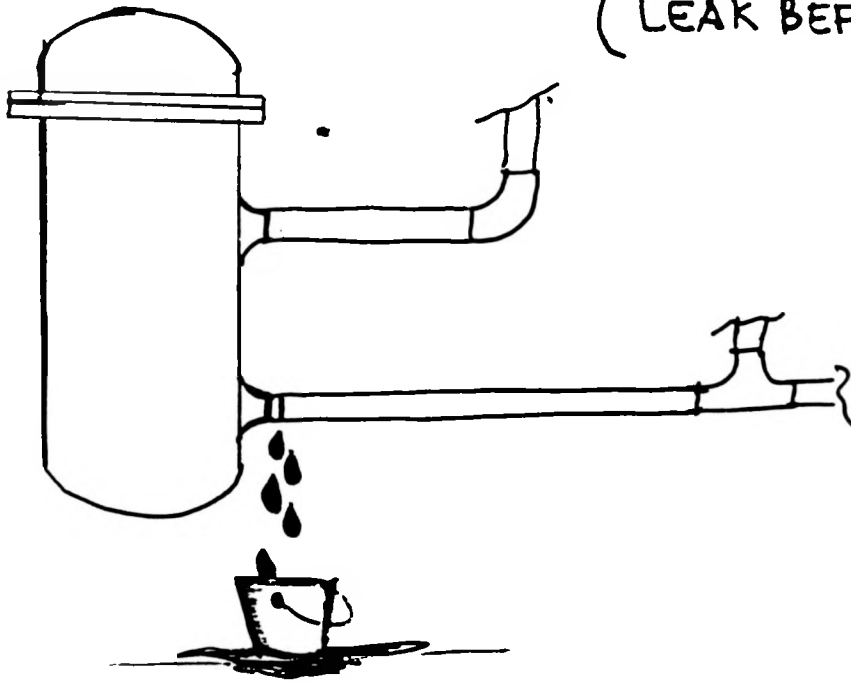
- . EXPERIMENTAL VERIFICATION OF PIPE FRACTURE BEHAVIOUR WITH AND WITHOUT CRACKS

- . VERIFICATION OF AVAILABLE ELASTIC-PLASTIC FM METHODS TO ASSESS PIPING INTEGRITY

- . VERIFICATION OF LBB CRITERION

- . MATERIALS DATA BANK

LBB CONCEPT
(LEAK BEFORE BREAK)



ALTERNATIVE LBB CONCEPT
(-LEAVE BEFORE BREAK)

N E D B P R O G R A M

M A T E R I A L

- . A 106 B

- . SA 316

- . SA 316 L

- . SA 316 WELDS

LOADING CONDITIONS

- . PRESSURE

- . FOUR POINT BEND TEST RIG

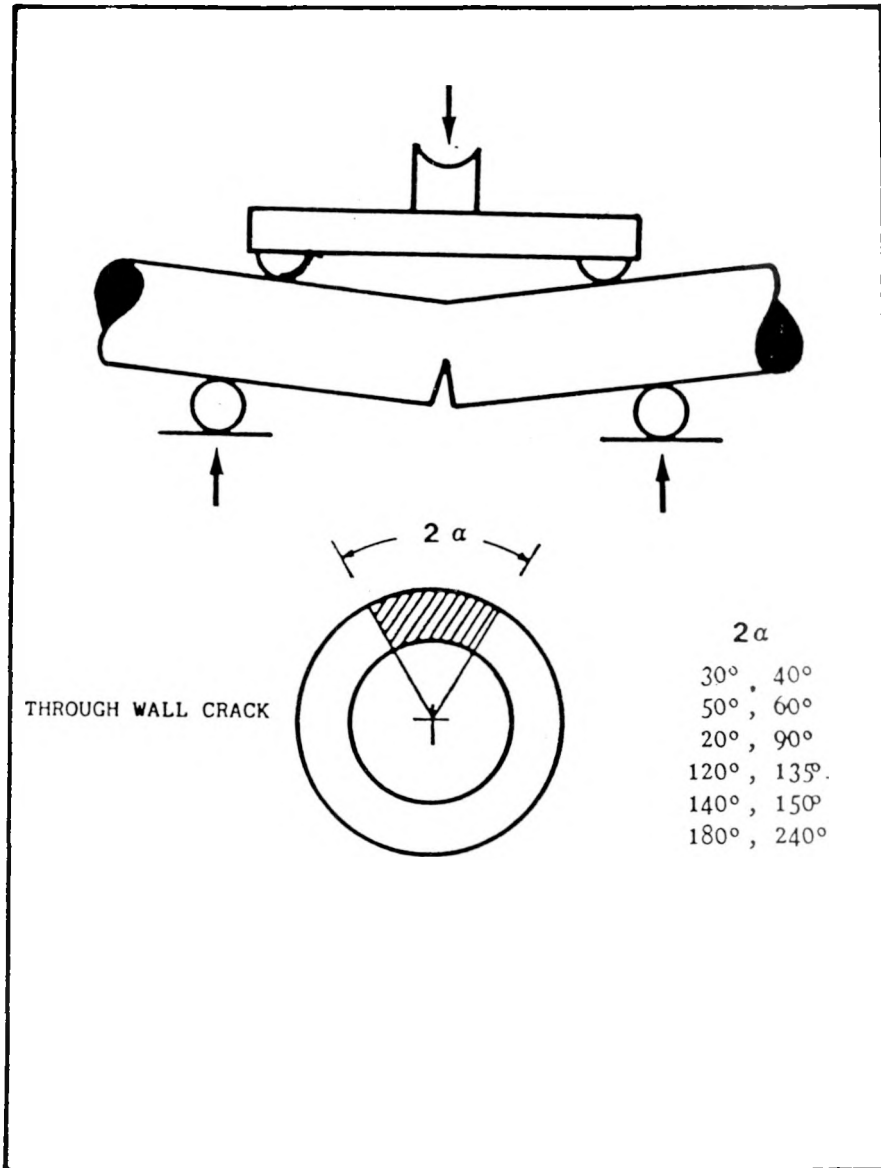


Figure 1 . Four point bend test method (pure bending), through crack

N E B D P R O G R A M

TEST MATRIX

N. OF TEST	MATERIAL	TEMP. °C	CRACK	CRACK ANGLE
69	A 106 I	RT&280°	PT-TW	20,30,45,55,60,65,85 90,95,135,175,185,360
5	SA 316	RT	TW	40,50,60,90,180,
14	SA 316 L	RT&280°	PT-TW	60,65,90,135,140,180 360
8	WELD SA 316 BASE	RT	PT-TW	90,135,180,360
96	TOTAL			

N E D B P R O G R A M

MAIN RESULTS

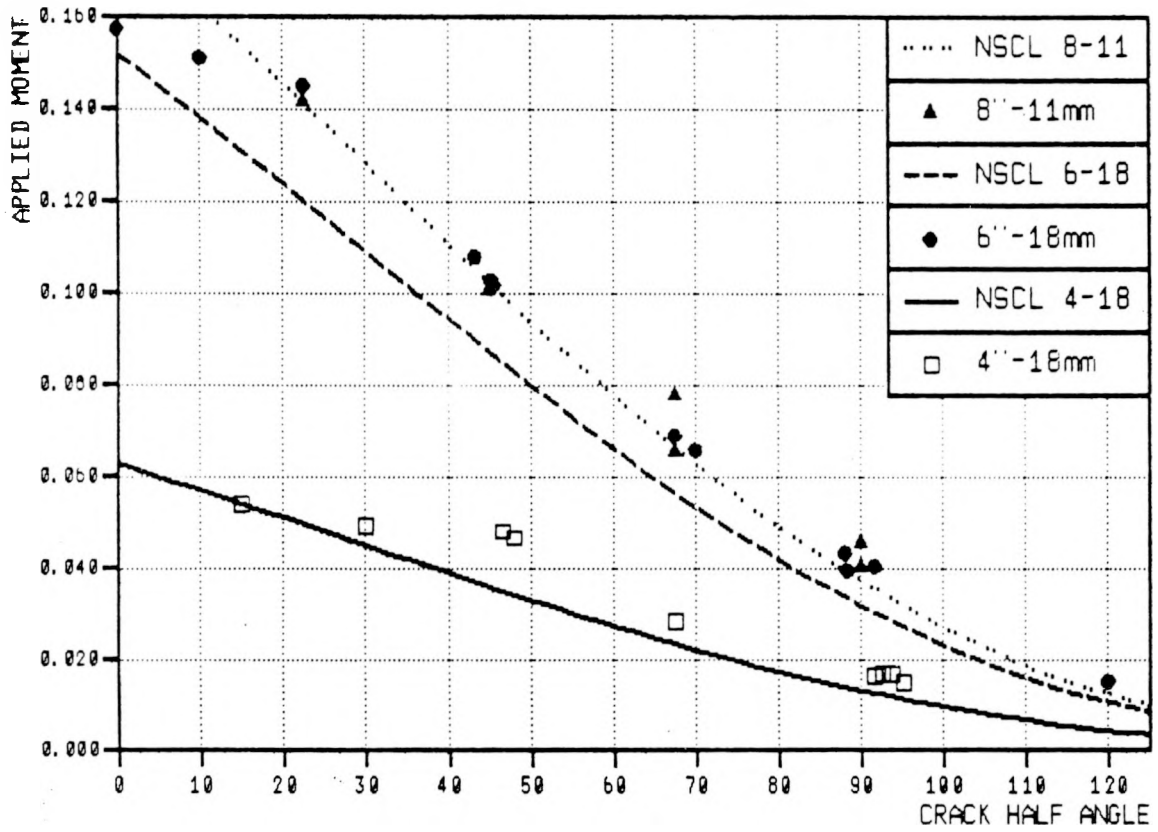
- . NSCL CRITERION SEEMS TO UNDERESTIMATE THE ACTUAL FRACTURE RESISTANCE OF CARBON STEEL PIPES WHILE IT ACCURATELY PREDICTS THAT OF STAINLESS STEEL PIPES

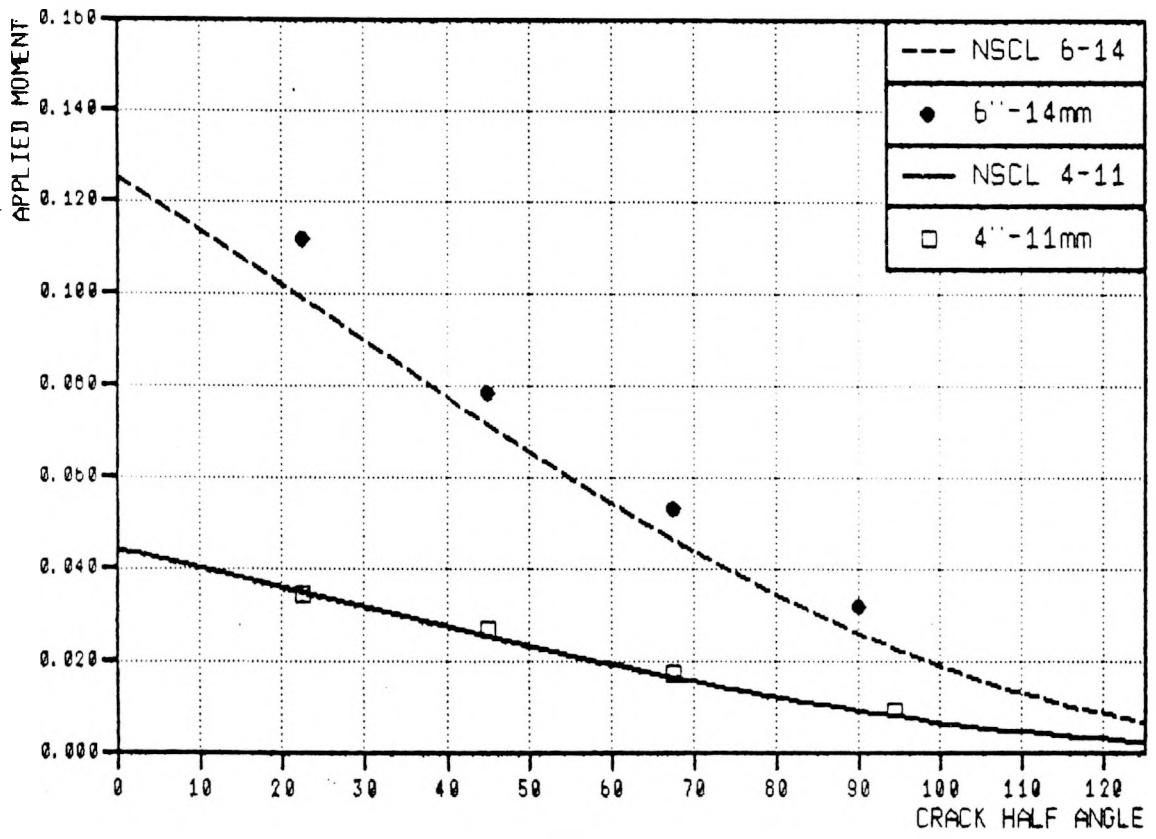
- . GE-EPRI ENGINEERING APPROACH IS A VALID TOOL TO PREDICT COD AND MAXIMUM MOMENT FOR PIPES

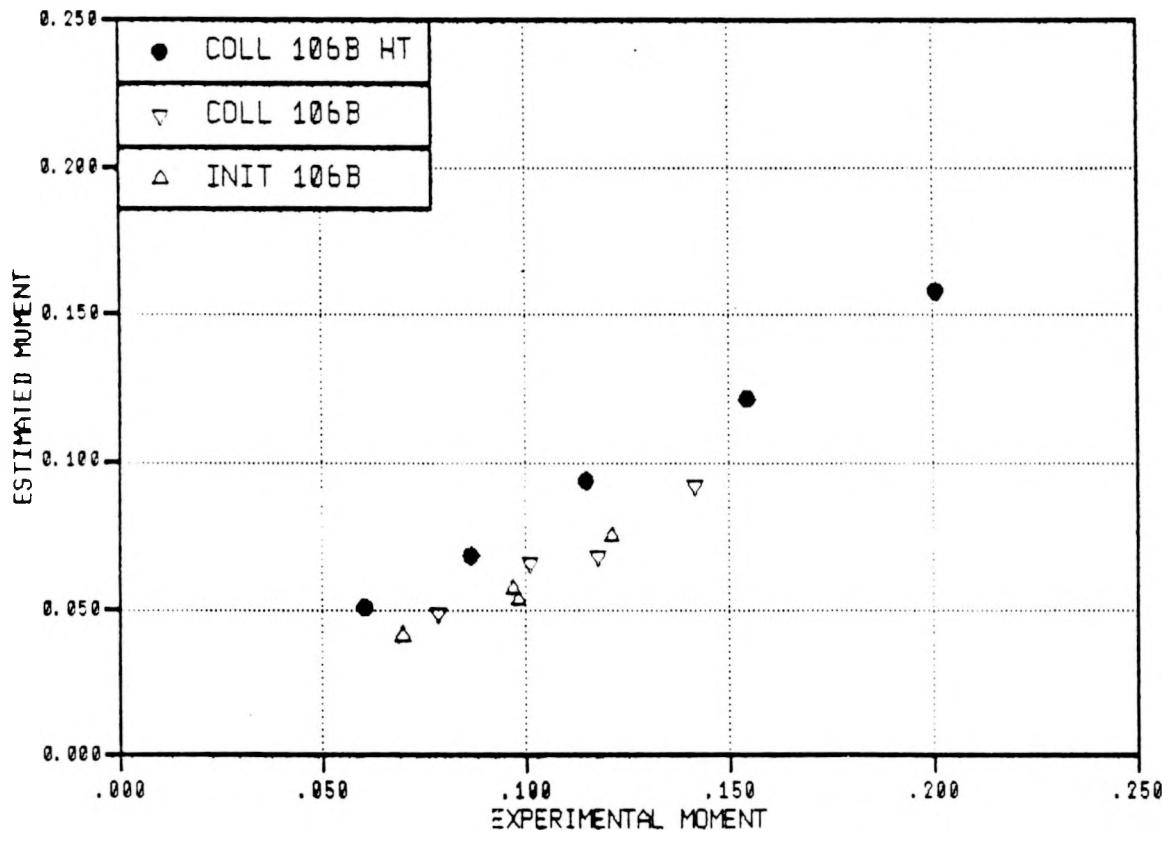
- . TADA-PARIS EQUATION SEEMS TO OVERESTIMATE THE ACTUAL LEAK AREA IN PIPES PARTICULARLY AT HIGH BENDING MOMENTS

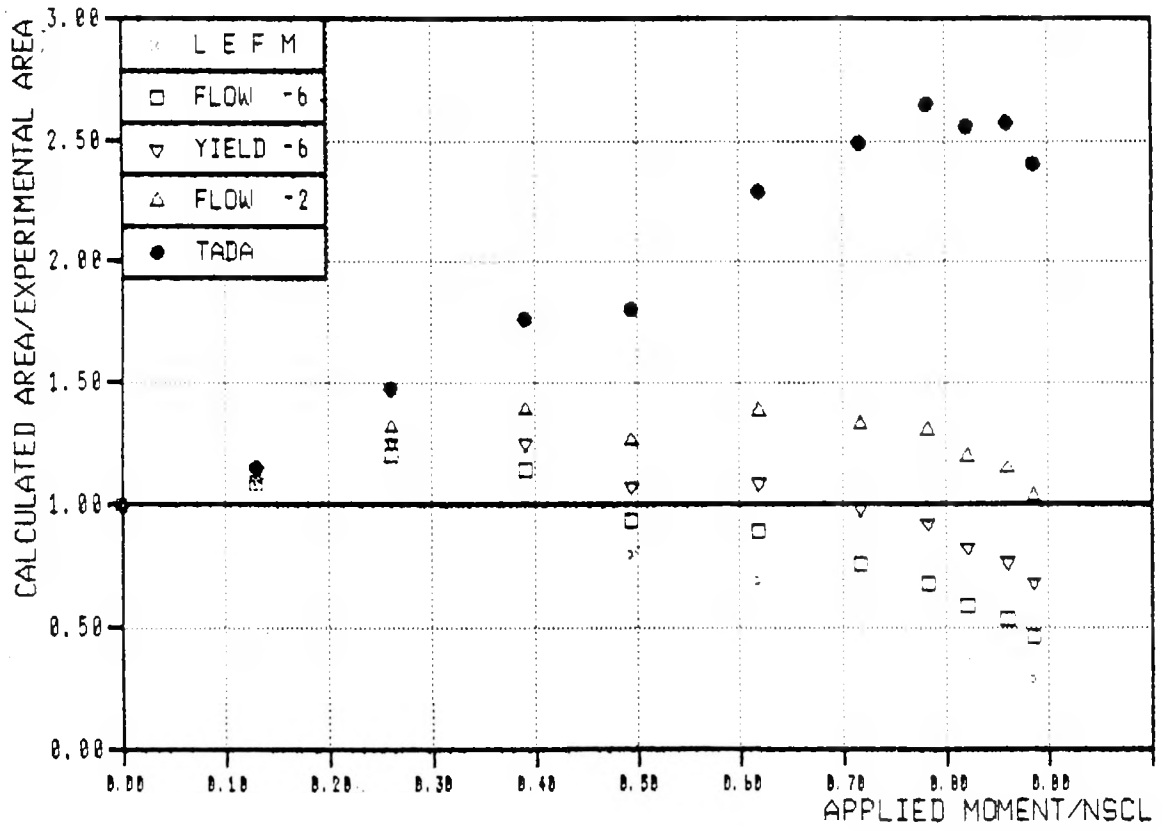
- . A-106 CAN UNDERGO SEVERE DYNAMIC-AGING AT 280°C

- . LEAK AREAS REMAIN WELL BELOW 10% OF PIPE CROSS SECTION WITH THROUGH-WALL CRACKS SHALLER THAN 160 DEG AND LOADS WITHIN ASME SEC III ALLOWABLE









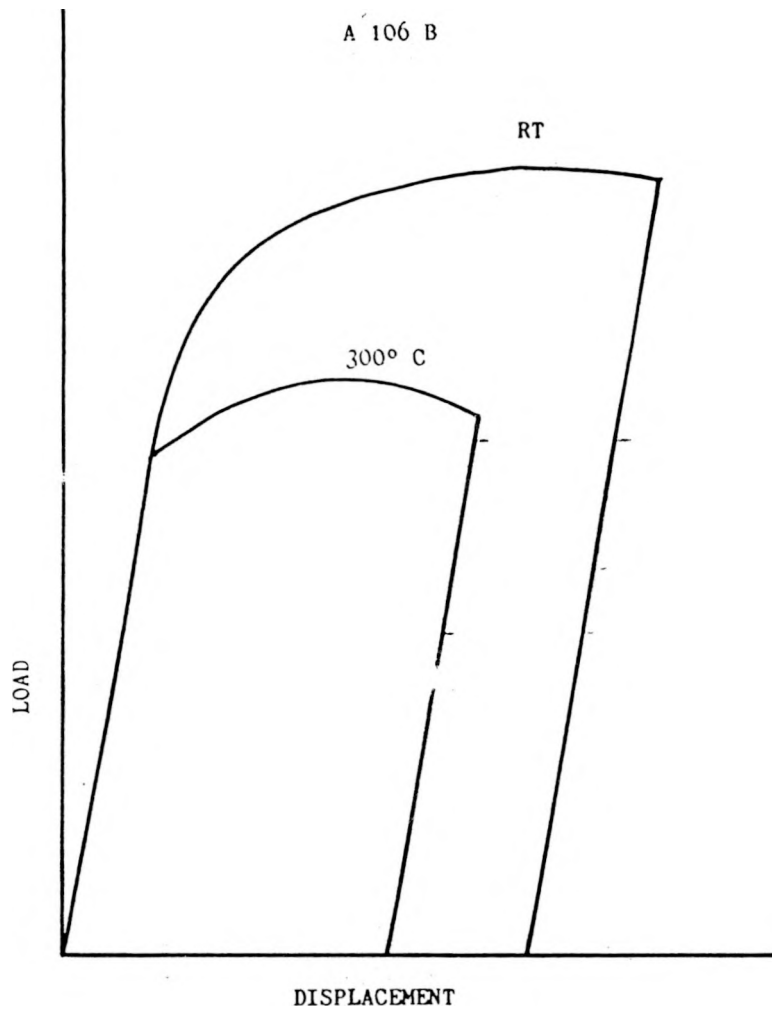
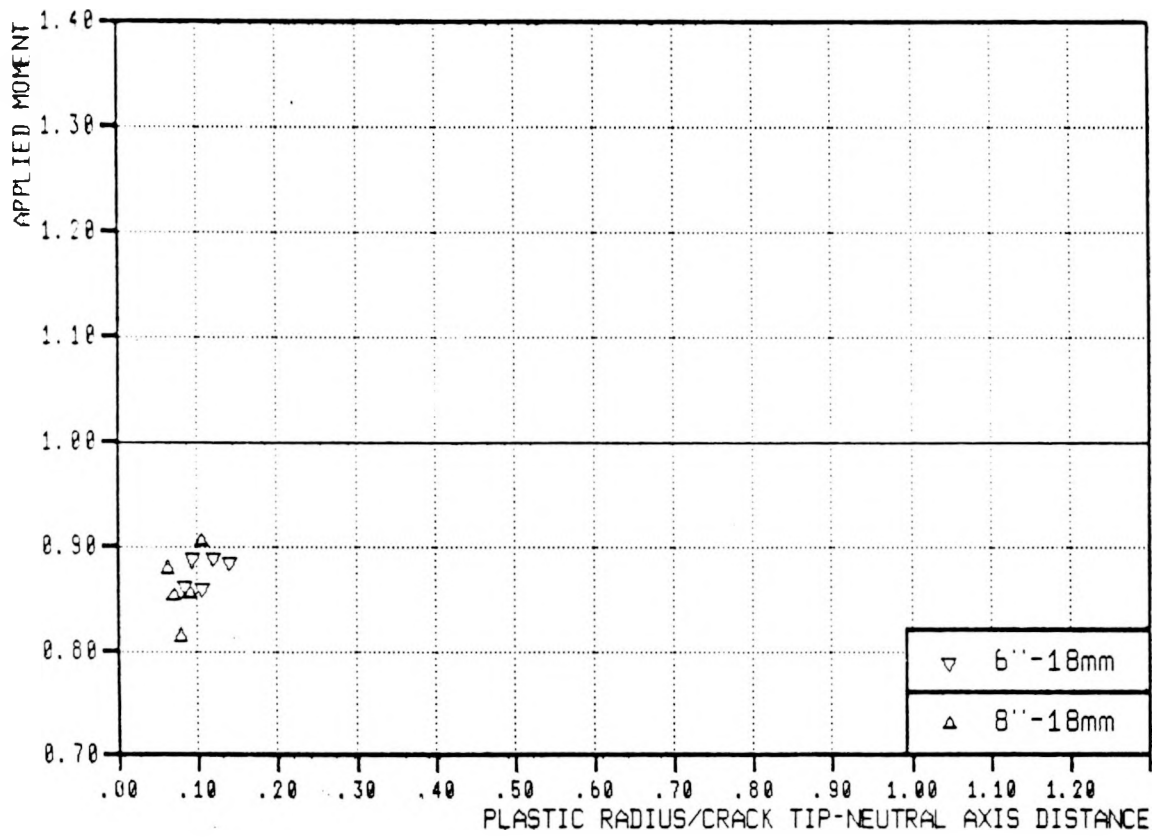


FIGURE 7

TABLE 2

Campione	J_{Ic} (KN/m)	K_{Jc} (MN/m ^{3/2})	TEARING MODULUS	Temp. (°C)
A4	334.03	277.78	570.8	20
A6	200.15	215.02	322.1	20
B6	236.29	233.63	275.3	20
C6	259.12	244.66	281.0	20
B6T	40.40	93.07	139.8	280
C6T	50.67	104.23	187.0	280
A8T	52.68	106.28	219.4	280
B8T	65.28	118.30	247.9	280

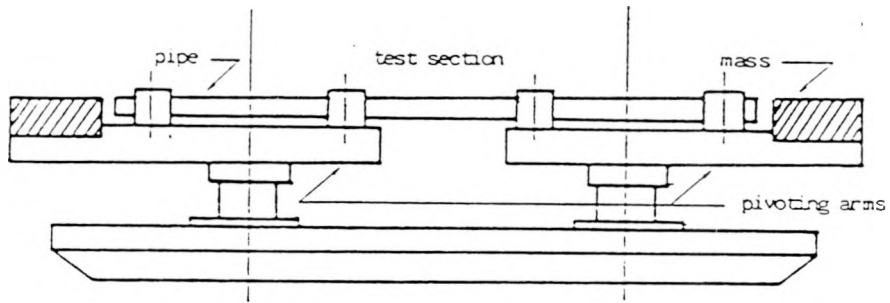


NEFQ PROGRAM

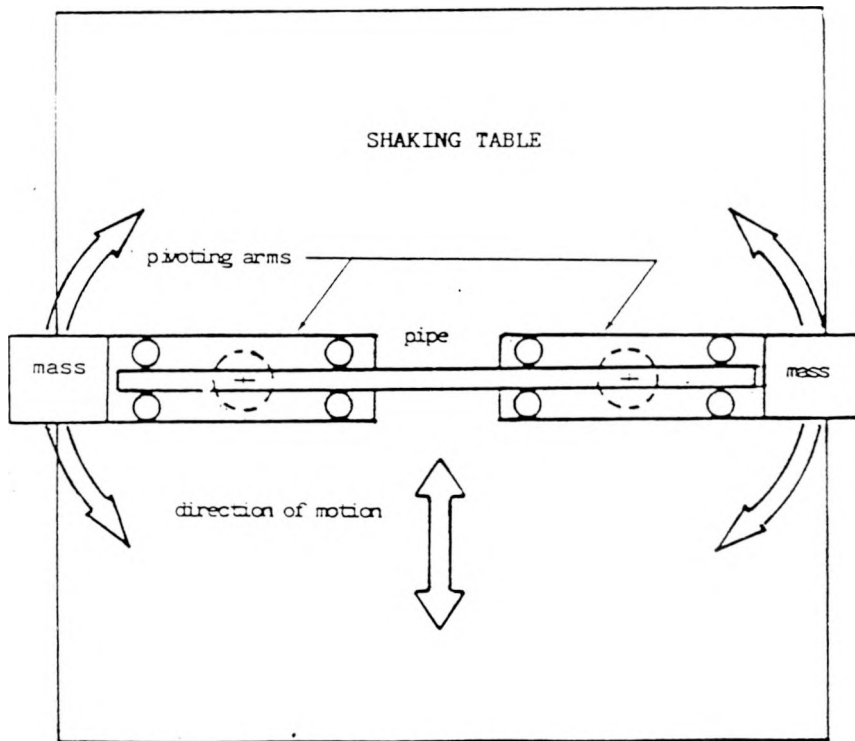
DURATION: 1988 - 1992

OBJECTIVE:

- . EXPERIMENTAL VERIFICATION OF PIPE FRACTURE BEHAVIOUR UNDER INERTIAL LOADS



SHAKING TABLE



SCHEMATIC OF INERTIAL LOAD TEST RIG

NEFQ PROGRAM

SHAKING TABLE CHARACTERISTICS

DIMENSION:	4 x 4 m
FREQUENCY RANGE:	0 - 50 Hz
MAX DISPLACEMENT:	250 MM (BELOW 0.62 Hz)
MAX VELOCITY:	50 CM/SEC
MAX ACCELERATION:	3 G
MAX FORCE:	64 T
MAX WEIGHT:	10 T

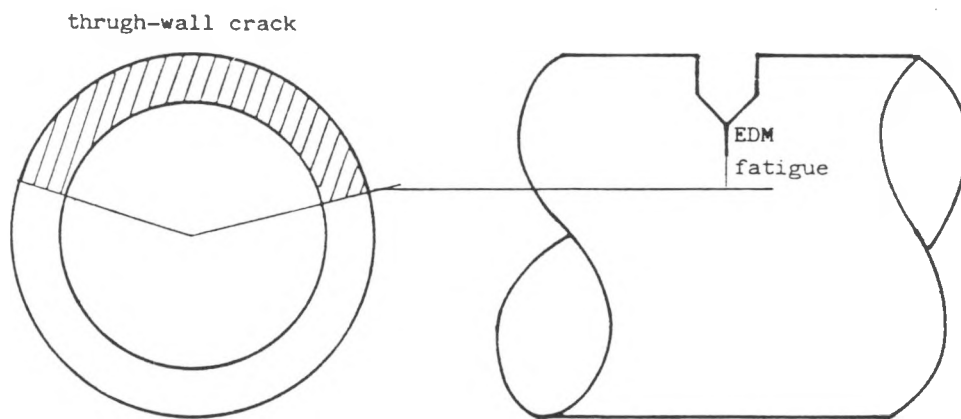
NEFQ PROGRAM

INITIAL TEST MATRIX (1 YEAR)

5 A 106 B PIPES, 4" - 11 MM THICK

R T

- . 1 PIPE WITHOUT CRACK
- . 3 PIPE WITH 140 DEG T-W FLAW, LARGE OPENING CRACK
- . 1 PIPE WITH 140 DEG T-W FLAW, CLOSE CRACK



SCHEMATIC OF CRACK CONFIGURATION

SESSION 3

CO-CHAIRMEN

**DR. K. KASHIMA
CRIEPI, JAPAN**

**MR. S. C. CHENG
TAIWAN POWER CO., TAIWAN**

**Deformation Mode and Crack Opening Displacement
for Circumferentially Cracked Pipes
in Bending and Tension**

S. H. Yoo

Division of Structures

Korean Institute of Machineries and Materials

Dae Jon Si, Chung Chong Nam Do, Korea

J. Pan

Mechanical Engineering and Applied Mechanics

The University of Michigan

Ann Arbor, Michigan 48109, U.S.A.

Summary

1. a brief review of the past research work on circumferentially cracked pipes using shell theory.
2. a parametric study of the displacement solutions of Sanders and Alabi.
3. conclusions.

Configurations

long circumferential through-wall cracks in long pipes and fixed-end long pipes.

Loading conditions

bending and tension

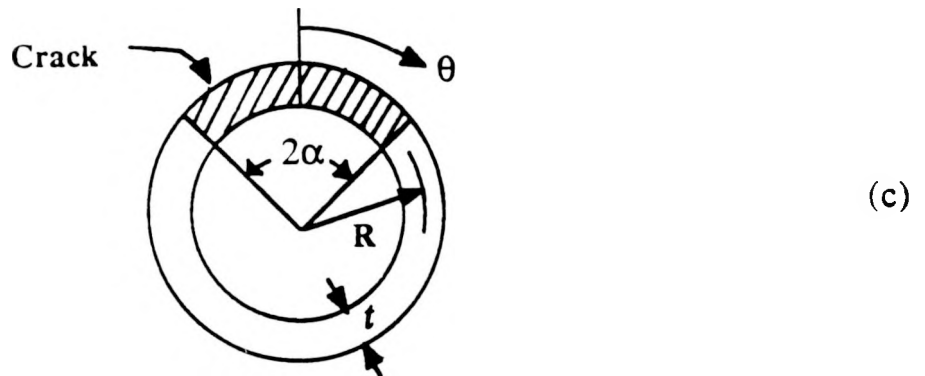
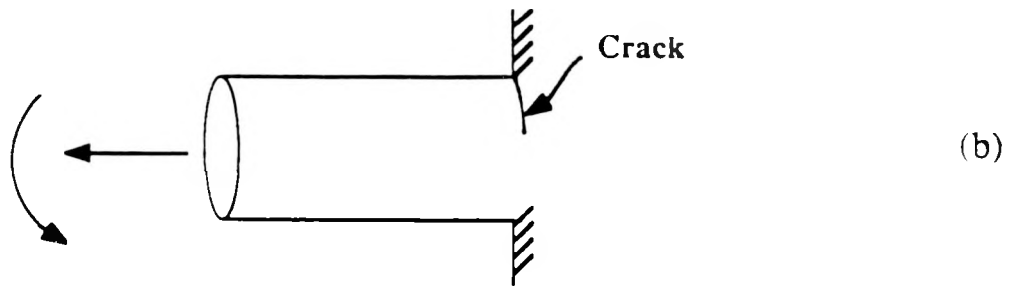
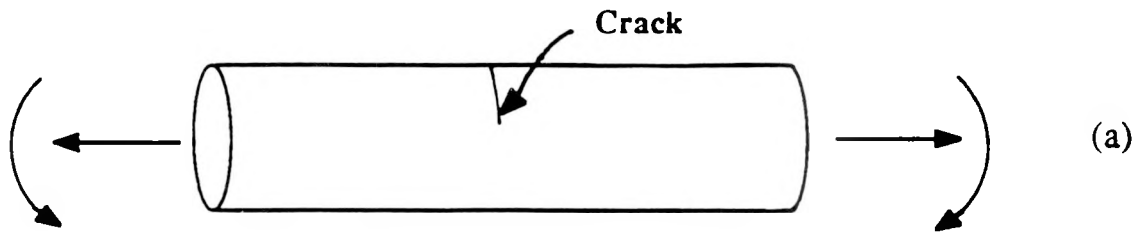


Figure 1 Circumferentially cracked pipes

Past research work using shell theories

A. Shallow shell theory for short circumferential cracks

Folias (1965, 1967): considered axial and circumferential cracks for small λ ($= [12(1 - \nu^2)]^{1/4} a / (Rt)^{1/2}$ where a is the half crack length).

Erdogan and his coworkers (1969, 1970, 1972): extended the range of the solutions for a larger λ and included the plasticity effects by Dugdale's modelling of the plastic zone.

Fama and Sanders (1972): confirmed the previous results.

Erdogan and Delale (1979): included the transverse shear effects.

B. Sanders' shell theory for long circumferential cracks

Sanders and Alabi (1983, 1984, 1985): for long pipes and fixed-end long pipes.

Nicholson, Weidman and Simmonds (1983): gave the values of Sanders' energy release rate for the whole range of λ ($= \alpha/2\epsilon$).

Sanders (1987): considered Dugdale's modelling of the plastic zone for plasticity effects and used the tearing instability theory of Paris et al. (1981) to determine the instability.

These research works are based on thin shell theories which are valid for $R/t > 20$. Most of these works centered on the energy release rates and tearing instability as well as the stress intensity factors for linear elastic materials.

Other computational works

Barsoum, Loomis and Stewart (1979): linear materials

The EPRI research work (1982, 1984): power-law materials

The NRC round-robin study (1986): nonlinear real material behavior.

and others.

These research works centered on the J calculation and the load-displacement relation.

Displacement fields from the shell theory solutions

1. the closed-form solutions in terms of elementary functions are easy for parametric studies.
2. the axial displacement can be used to estimate the crack opening displacement for leak-rate calculation.
3. the normal displacement can be used to understand the deformation mode which can indicate the trend of the moment carrying capacity when finite deformation is considered.

Shell equations for cylinders

Notation:

$$(\) \cdot = \frac{\partial}{\partial \theta}$$

$$(\)' = \frac{\partial}{\partial z}$$

$$\epsilon^2 = \frac{t}{\sqrt{12(1 - \nu^2)}R}$$

where θ represents the circumferential coordinate and z represents the normalized axial coordinate by the mean radius R .

Love (1920): started a system of shell equations.

Flügge (1932): established the most complicated shell equations of the first order.

Donnell (1933, 1938): gave two simple eighth-order differential equations in term of the normal displacement w .

$$\nabla^8 w + \epsilon^{-2} w'''' = 0$$

$$\nabla^8 w + 2w'''' + w'''' + \epsilon^{-2} w'''' = 0$$

Morley (1958): gave a shell equation which is more accurate than those of Donnell but simpler than that of Flugge.

$$\nabla^4(\nabla^2 + 1)^2 w + \epsilon^{-2} w'''' = 0$$

Novozhilov (1946, appear in English in 1959): He reduced the order of shell equations by formulating the equations in complex form.

$$\nabla^4 \tilde{T} + \tilde{T} - i\epsilon^{-1} \tilde{T}'' = 0$$

$$\tilde{T} = N_z + N_\theta - i(\epsilon EtR)(\chi_z + \chi_\theta)$$

where N_z and N_θ are force resultants and χ_z and χ_θ are strain quantities.

Sanders (1970): He modified Novozhilov's equations as

$$\epsilon^2 u = \ddot{\Phi}' - \epsilon^2 \nu \varphi'$$

$$\epsilon^2 v = -\dot{\Phi} - \epsilon^2 (2 + \nu) \dot{\varphi}$$

$$w = -\ddot{\Phi} + i(1 + 2i\epsilon^2) \varphi$$

$$\epsilon^2 \chi_z = -i\ddot{\Phi}' - i\epsilon^2 \nu \varphi'$$

$$\epsilon^2 \chi_\theta = i\dot{\Phi} + i\epsilon^2 (2 - \nu) \dot{\varphi}$$

$$\psi = i\ddot{\Phi} + (1 + 2i\epsilon^2) \varphi$$

$$\nabla^4 \varphi + \ddot{\varphi} - i\epsilon^{-2} (1 + 2i\epsilon^2) \varphi'' = 0$$

$$\Phi'' = \epsilon^2 \varphi$$

where u , v , w are the normalized in-plane displacements and normal displacement at the middle surface, and χ_z , χ_θ and ψ are the stress functions which should comply to the boundary conditions.

The physical quantities are the real parts of the complex quantities.

Boundary conditions

Traction-free conditions on the crack surface require:

$$\chi_z = \chi_\theta = \psi = \psi' = 0$$

Symmetry conditions for infinitely long pipes require:

$$\chi_z = \psi' = 0, \quad u = w' = 0$$

The physical nondimensional quantities vanish at $z = \infty$.

Solutions

Complete solutions = Nominal solutions + Elementary solutions + Semi-membrane solutions + Edge-effect solutions.

Nominal solutions for bending are given

$$\varphi = -\cos \theta$$
$$\Phi = -\frac{1}{2}\epsilon^2 z^2 \cos \theta + \frac{1}{2}i\theta \sin \theta$$

Nominal solutions for tension are given

$$\varphi = \frac{1}{2}(1 + i\epsilon^2\nu)\theta^2 - \frac{1}{2}i\epsilon^2[1 - i\epsilon^2(2 - \nu)]z^2$$
$$\ddot{\Phi} = \frac{1}{2}i[1 + i\epsilon^2(2 + \nu)]\theta^2 + (1 + i\epsilon^2\nu)\left(\frac{1}{2}\epsilon^2 z^2 - i\right)$$

Elementary solutions are needed to match the conditions at $z = \infty$.

Rigid body motion
(Axial and lateral translation and rotation)

Null solution

Semi-membrane solutions count mainly for the in-plane effects.

$$(\ddot{\varphi} + \varphi)'' - i\epsilon^{-2}\varphi'' = 0$$

Edge-effect solutions represent for the out-of-plane effects.

$$\varphi'' - i\epsilon^{-2}\varphi = 0$$

Cracked pipes in bending

$$u = \begin{cases} \epsilon^{-1} \left\{ -B + D \cos \theta + \frac{\sqrt{2}}{2} (-A + C \cos \theta + 3 \cos \theta - \frac{1}{2} \theta \sin \theta) \right\} & \text{on the crack;} \\ 0 & \text{off the crack,} \end{cases}$$

$$w = \begin{cases} (2 + C) \cos \theta - \frac{1}{2} \theta \sin \theta & \text{on the crack;} \\ -\sqrt{2} D \cos \theta - \frac{1}{2} P \cos \frac{\pi - \theta}{\sqrt{2}} & \text{off the crack,} \end{cases}$$

and

$$u_M = \frac{\sqrt{2}}{4} \epsilon^{-1} \left\{ \alpha \tan \alpha + (1 - \cos \alpha) \left(2 - \frac{\alpha \sec^2 \alpha - \tan \alpha}{\tan \alpha + \sqrt{2} \tan \frac{\pi - \alpha}{\sqrt{2}}} \right) \right\},$$

where

$$\Delta = -\frac{\pi}{\sqrt{2}} \cos \alpha$$

$$P = \frac{\alpha - \frac{1}{2} \sin 2\alpha}{\sin \alpha \cos \frac{\pi - \alpha}{\sqrt{2}} + \sqrt{2} \cos \alpha \sin \frac{\pi - \alpha}{\sqrt{2}}}$$

$$A = \cos \alpha - \sqrt{2} B - \frac{1}{2} P \cos \frac{\pi - \alpha}{\sqrt{2}}$$

$$B = -\frac{1}{2\sqrt{2}\pi} P \left(\sqrt{2} \sin \frac{\pi - \alpha}{\sqrt{2}} + \alpha \cos \frac{\pi - \alpha}{\sqrt{2}} \right) - \frac{1}{\sqrt{2}\pi} (\sin \alpha - \alpha \cos \alpha)$$

$$\begin{aligned} \Delta C = & \frac{\sqrt{2}}{4} P \cos \frac{\pi - \alpha}{\sqrt{2}} \left(\pi - \alpha + \frac{1}{2} \sin 2\alpha \right) + \frac{\sqrt{2}}{4} \left[\alpha(\alpha - \pi) \sin \alpha + 4\pi \cos \alpha \right. \\ & \left. + \frac{5}{4} (2\alpha - \sin 2\alpha) \cos \alpha \right] \end{aligned}$$

$$\Delta D = (2\alpha - \sin 2\alpha) \left(\frac{1}{8} P \cos \frac{\pi - \alpha}{\sqrt{2}} - \frac{5}{16} \cos \alpha \right) - \frac{1}{4} \alpha^2 \sin \alpha.$$

Cracked pipes in tension

$$u = \begin{cases} -\epsilon^{-1} \left\{ \frac{\sqrt{2}}{2} (1 - C \cos \theta + \frac{1}{2} \theta^2 + A) + B - D \cos \theta \right\} & \text{on the crack;} \\ 0 & \text{off the crack,} \end{cases}$$

$$w = \begin{cases} -1 + C \cos \theta & \text{on the crack;} \\ -\sqrt{2} D \cos \theta - \frac{1}{2} P \cos \frac{\pi - \theta}{\sqrt{2}} & \text{off the crack,} \end{cases}$$

and

$$u_M = \frac{\sqrt{2}}{4} \epsilon^{-1} \left\{ \alpha^2 + 2 \frac{1 - \cos \alpha}{\cos \alpha} \frac{\alpha + \sqrt{2} \tan \frac{\pi - \alpha}{\sqrt{2}}}{\tan \alpha + \sqrt{2} \tan \frac{\pi - \alpha}{\sqrt{2}}} \right\},$$

where

$$\Delta = \sin \alpha \cos \frac{\pi - \alpha}{\sqrt{2}} + \sqrt{2} \cos \alpha \sin \frac{\pi - \alpha}{\sqrt{2}}$$

$$\beta = \sin \frac{\pi - \alpha}{\sqrt{2}} + \frac{\alpha}{\sqrt{2}} \cos \frac{\pi - \alpha}{\sqrt{2}}$$

$$\Delta P = 2(\sin \alpha - \alpha \cos \alpha)$$

$$\pi A = \frac{1}{3} \alpha^3 - \frac{\pi}{2} \alpha^2 + \frac{\sqrt{2}}{2} \left[\sin \frac{\pi - \alpha}{\sqrt{2}} - \left(\frac{\pi - \alpha}{\sqrt{2}} \right) \cos \frac{\pi - \alpha}{\sqrt{2}} \right] P$$

$$2\pi B = -\left(\frac{\sqrt{2}}{3} \alpha^3 + \beta P \right)$$

$$\Delta C = \sqrt{2}(\beta - \Delta D)$$

$$2\pi \Delta D = (\sin 2\alpha - 2\alpha \cos 2\alpha) \sin \frac{\pi - \alpha}{\sqrt{2}} + \sqrt{2}(\alpha^2 + 2 \sin^2 \alpha - 3\alpha \sin \alpha \cos \alpha) \cos \frac{\pi - \alpha}{\sqrt{2}}$$

Cracked pipes with fixed end in bending

$$u = \epsilon^{-1} \left\{ -b_R - b_I + (d_R + d_I) \cos \theta + \frac{\sqrt{2}}{2} (-a_R + c_R \cos \theta - \frac{1}{2} \theta \sin \theta + 3 \cos \theta) \right\},$$

$$w = -\frac{1}{2} \theta \sin \theta + (c_R + c_I + 2 + 2\sqrt{2}d_I) \cos \theta - \frac{A}{2} \cos \frac{\theta}{\sqrt{2}},$$

$$u_M = \frac{\sqrt{2}}{4} \epsilon^{-1} \left\{ \alpha \tan \alpha + (1 - \cos \alpha) \left[2 - \frac{1}{2} \left(\frac{\alpha \sec^2 \alpha - \tan \alpha}{\sqrt{2} \tan \frac{\pi - \alpha}{\sqrt{2}} + \tan \alpha} - \frac{\alpha \sec^2 \alpha - \tan \alpha}{\sqrt{2} \tan \frac{\alpha}{\sqrt{2}} - \tan \alpha} \right) \right] \right\},$$

where

$$A = \frac{\alpha - \cos \alpha \sin \alpha}{\sqrt{2} \cos \alpha \sin \frac{\alpha}{\sqrt{2}} - \sin \alpha \cos \frac{\alpha}{\sqrt{2}}}$$

$$B = \frac{\alpha - \cos \alpha \sin \alpha}{\sqrt{2} \cos \alpha \sin \frac{\pi - \alpha}{\sqrt{2}} + \sin \alpha \cos \frac{\pi - \alpha}{\sqrt{2}}}$$

$$\pi a_I = \alpha \cos \alpha - \sin \alpha + A \left(\frac{\alpha}{2} \cos \frac{\alpha}{\sqrt{2}} - \frac{1}{\sqrt{2}} \sin \frac{\alpha}{\sqrt{2}} \right)$$

$$2\sqrt{2}\pi b_R = \alpha \cos \alpha - \sin \alpha + \pi a_I - B \left(\frac{1}{\sqrt{2}} \sin \frac{\pi - \alpha}{\sqrt{2}} + \frac{\alpha}{\sqrt{2}} \cos \frac{\pi - \alpha}{\sqrt{2}} \right)$$

$$a_R = \cos \alpha - 2\sqrt{2}b_R + a_I - \frac{B}{2} \cos \frac{\pi - \alpha}{\sqrt{2}}$$

$$2\sqrt{2}b_I = -a_R - a_I + \cos \alpha + \frac{A}{2} \cos \frac{\alpha}{\sqrt{2}}$$

$$\frac{\pi}{2} c_I \sin \alpha = a_R \sin^2 \alpha - \frac{1}{4} (\alpha + \cos \alpha \sin \alpha) (3 \sin \alpha + \alpha \cos \alpha) + \frac{\alpha}{8} \sin \alpha (4 + \cos 2\alpha)$$

$$+ \frac{3}{8} \cos \alpha \sin^2 \alpha + 2\sqrt{2}b_I \sin^2 \alpha + a_I \sin^2 \alpha + A\Gamma$$

$$\pi\sqrt{2}d_R \sin \alpha = \pi c_I \sin \alpha + (-2a_I - 2\sqrt{2}b_I + 2\sqrt{2}b_R) \sin^2 \alpha + B\Delta - A\Gamma$$

$$c_R \cos \alpha = -2 \cos \alpha + \frac{1}{2} \alpha \sin \alpha + c_I \cos \alpha - 2\sqrt{2}d_R \cos \alpha - \frac{B}{2} \cos \frac{\pi - \alpha}{\sqrt{2}}$$

$$2\sqrt{2}d_I \cos \alpha = -2 \cos \alpha + \frac{1}{2} \alpha \sin \alpha - c_R \cos \alpha - c_I \cos \alpha + \frac{A}{2} \cos \frac{\alpha}{\sqrt{2}}$$

$$\Gamma = -2 \sin^2 \alpha \cos \frac{\alpha}{\sqrt{2}} + \frac{1}{2\sqrt{2}} \sin \frac{\alpha}{\sqrt{2}} (\alpha + 5 \cos \alpha \sin \alpha)$$

$$\Delta = 2 \sin^2 \alpha \cos \frac{\pi - \alpha}{\sqrt{2}} + \frac{1}{2\sqrt{2}} \sin \frac{\pi - \alpha}{\sqrt{2}} (\alpha + 5 \cos \alpha \sin \alpha).$$

Cracked pipes with fixed end in tension

$$u = \epsilon^{-1} \left\{ -b_R - b_I + (d_R + d_I) \cos \theta + \frac{\sqrt{2}}{2} \left(-1 - \frac{1}{2} \theta^2 - a_R + c_R \cos \theta \right) \right\},$$

$$w = -1 + (c_R + c_I) \cos \theta + 2\sqrt{2}d_I \cos \theta - \frac{A}{2} \cos \frac{\theta}{\sqrt{2}},$$

and

$$u_M = \frac{\sqrt{2}}{4} \epsilon^{-1} \left\{ \alpha^2 + \frac{1 - \cos \alpha}{\cos \alpha} \left[2 - \frac{\tan \alpha - \alpha}{\sqrt{2} \tan \frac{\pi - \alpha}{\sqrt{2}} + \tan \alpha} + \frac{\tan \alpha - \alpha}{\sqrt{2} \tan \frac{\alpha}{\sqrt{2}} - \tan \alpha} \right] \right\},$$

where

$$A = \frac{\sin \alpha - \alpha \cos \alpha}{\frac{1}{\sqrt{2}} \cos \alpha \sin \frac{\alpha}{\sqrt{2}} - \frac{1}{2} \sin \alpha \cos \frac{\alpha}{\sqrt{2}}}$$

$$B = \frac{\sin \alpha - \alpha \cos \alpha}{\frac{1}{\sqrt{2}} \cos \alpha \sin \frac{\pi - \alpha}{\sqrt{2}} + \frac{1}{2} \sin \alpha \cos \frac{\pi - \alpha}{\sqrt{2}}}$$

$$\pi a_I = -\frac{1}{3} \alpha^3 + A \left(\frac{\alpha}{2} \cos \frac{\alpha}{\sqrt{2}} - \frac{1}{\sqrt{2}} \sin \frac{\alpha}{\sqrt{2}} \right)$$

$$2\sqrt{2} \pi b_R = -\frac{1}{3} \alpha^3 + \pi a_I - B \left(\frac{\alpha}{2} \cos \frac{\pi - \alpha}{\sqrt{2}} + \frac{1}{\sqrt{2}} \sin \frac{\pi - \alpha}{\sqrt{2}} \right)$$

$$a_R = -\frac{1}{2} \alpha^2 - 2\sqrt{2} b_R + a_I - \frac{B}{2} \cos \frac{\pi - \alpha}{\sqrt{2}}$$

$$2\sqrt{2} b_I = -\frac{1}{2} \alpha^2 - a_R - a_I + \frac{A}{2} \cos \frac{\alpha}{\sqrt{2}}$$

$$\frac{\pi}{2} c_I \sin \alpha = \left\{ a_R + \frac{1}{2} (\alpha^2 - 4) + 2\sqrt{2} b_I + a_I \right\} \sin^2 \alpha + \frac{\alpha}{2} (\alpha + 3 \cos \alpha \sin \alpha) + A \Gamma$$

$$\pi \sqrt{2} d_R \sin \alpha = \pi c_I \sin \alpha + (-2a_I - 2\sqrt{2} b_I + 2\sqrt{2} b_R) \sin^2 \alpha + B \Delta - A \Gamma$$

$$c_R \cos \alpha = 1 + c_I \cos \alpha - 2\sqrt{2} d_R \cos \alpha - \frac{B}{2} \cos \frac{\pi - \alpha}{\sqrt{2}}$$

$$2\sqrt{2} d_I \cos \alpha = 1 - c_R \cos \alpha + \frac{A}{2} \cos \frac{\alpha}{\sqrt{2}} - c_I \cos \alpha$$

$$\Gamma = -2 \sin^2 \alpha \cos \frac{\alpha}{\sqrt{2}} + \frac{1}{2\sqrt{2}} \sin \frac{\alpha}{\sqrt{2}} (\alpha + 5 \cos \alpha \sin \alpha)$$

$$\Delta = 2 \sin^2 \alpha \cos \frac{\pi - \alpha}{\sqrt{2}} + \frac{1}{2\sqrt{2}} \sin \frac{\pi - \alpha}{\sqrt{2}} (\alpha + 5 \cos \alpha \sin \alpha)$$

Cracked pipes in bending and tension

The dimensionless variables are related to the physical axial and normal displacement, U and W , by

$$U = \frac{\sigma R}{E} u$$
$$W = \frac{\sigma R}{E \epsilon^2} w$$

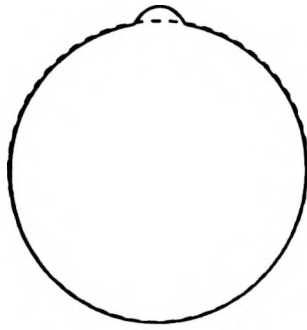
where σ is the nominal stress and E is Young's modulus.

The nominal stress σ for the bending cases is defined as:

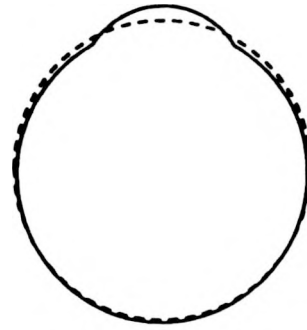
$$\sigma = \frac{M}{\pi R^2 t} = \frac{MR}{I}$$

where M is the remote bending moment, and I is the moment of inertia of the pipe cross section.

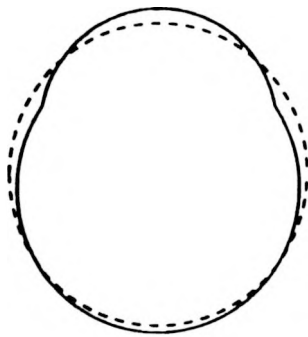
The nominal stress σ for the tension cases is defined as the remote uniformly applied tensile stress.



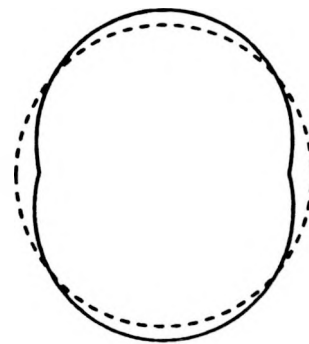
(a)



(b)



(c)



(d)

Figure 2 Normalized normal displacements of an infinite long pipe with a circumferential crack subjected to remote bending.
(a) $\alpha = 10^\circ$ (b) $\alpha = 30^\circ$ (c) $\alpha = 60^\circ$ (d) $\alpha = 90^\circ$

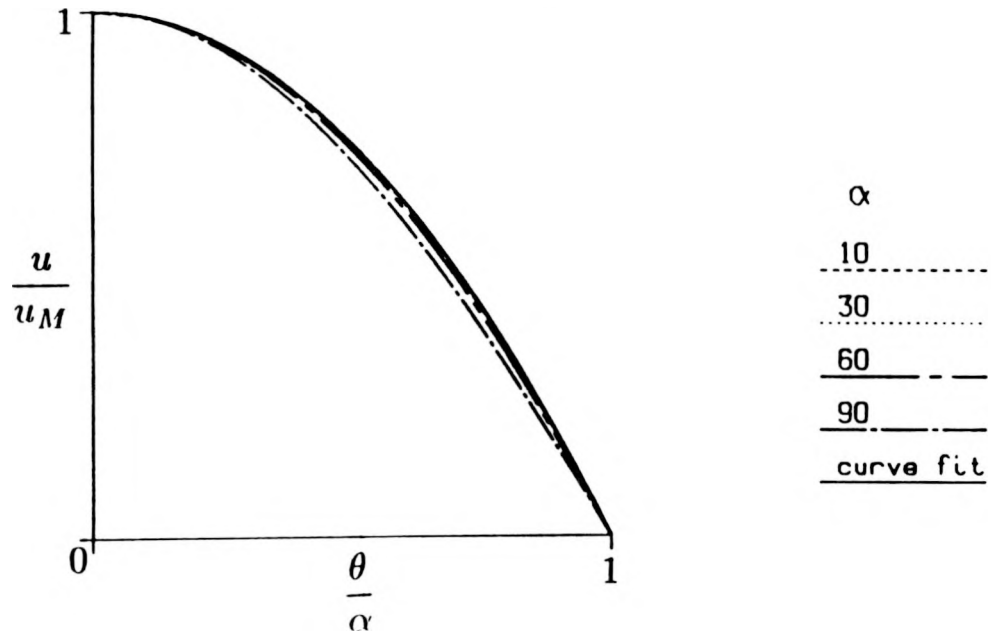
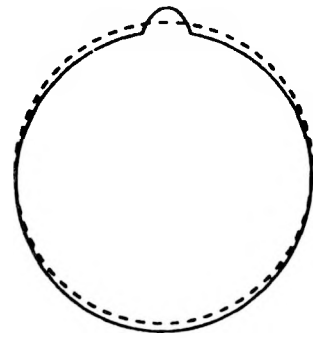
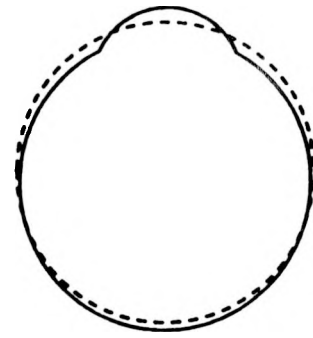


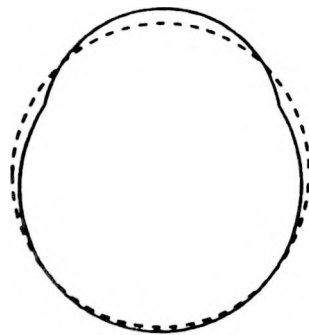
Figure 3 Normalized axial displacements of an infinite long pipe with a circumferential crack subjected to remote bending.



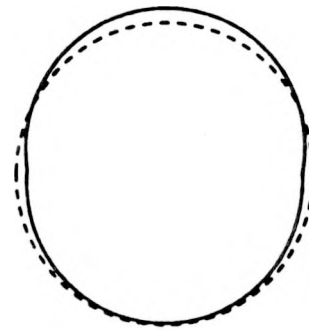
(a)



(b)



(c)



(d)

Figure 4 Normalized normal displacements of an infinite long pipe with a circumferential crack subjected to uniform remote tension. (a) $\alpha = 10^\circ$ (b) $\alpha = 30^\circ$ (c) $\alpha = 60^\circ$ (d) $\alpha = 90^\circ$

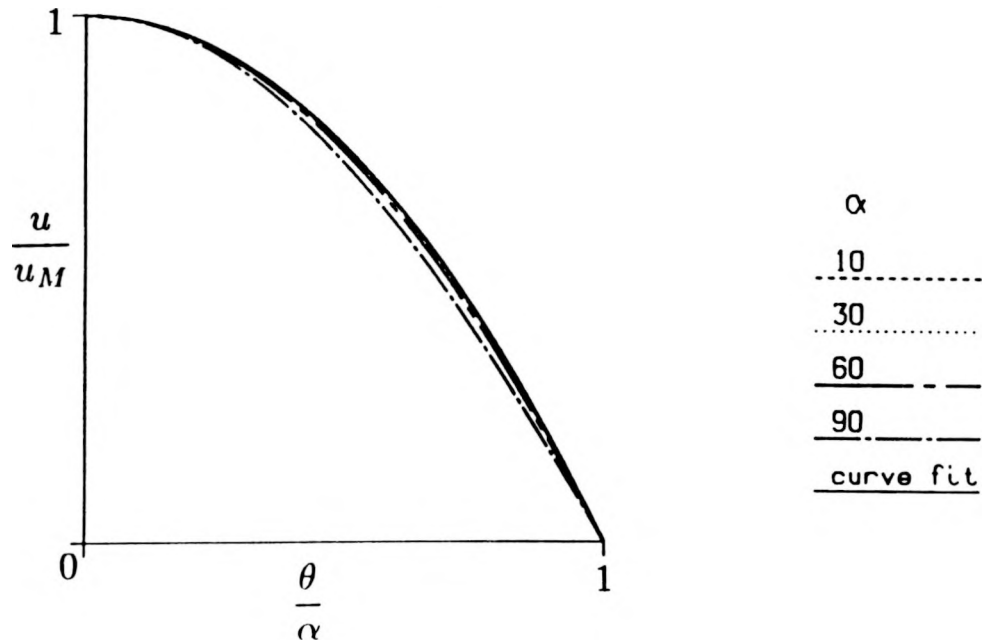
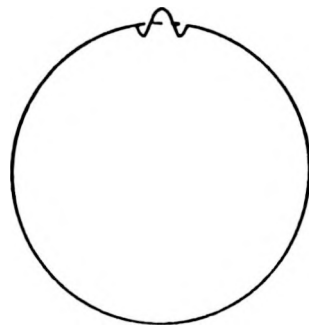
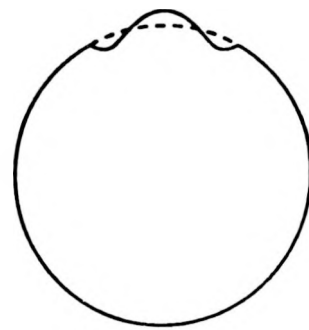


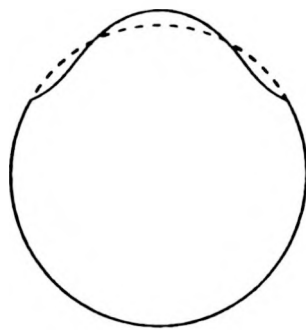
Figure 5 Normalized axial displacements of an infinite long pipe with a circumferential crack subjected to uniform remote tension.



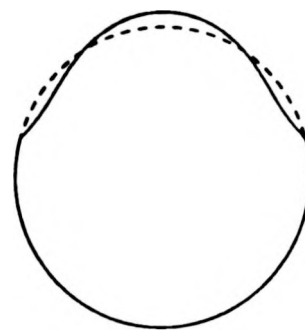
(a)



(b)



(c)



(d)

Figure 6 Normalized normal displacements of a semi-infinite long pipe with a circumferential crack at the fixed end subjected to remote bending.
(a) $\alpha = 10^\circ$ (b) $\alpha = 30^\circ$ (c) $\alpha = 60^\circ$ (d) $\alpha = 90^\circ$

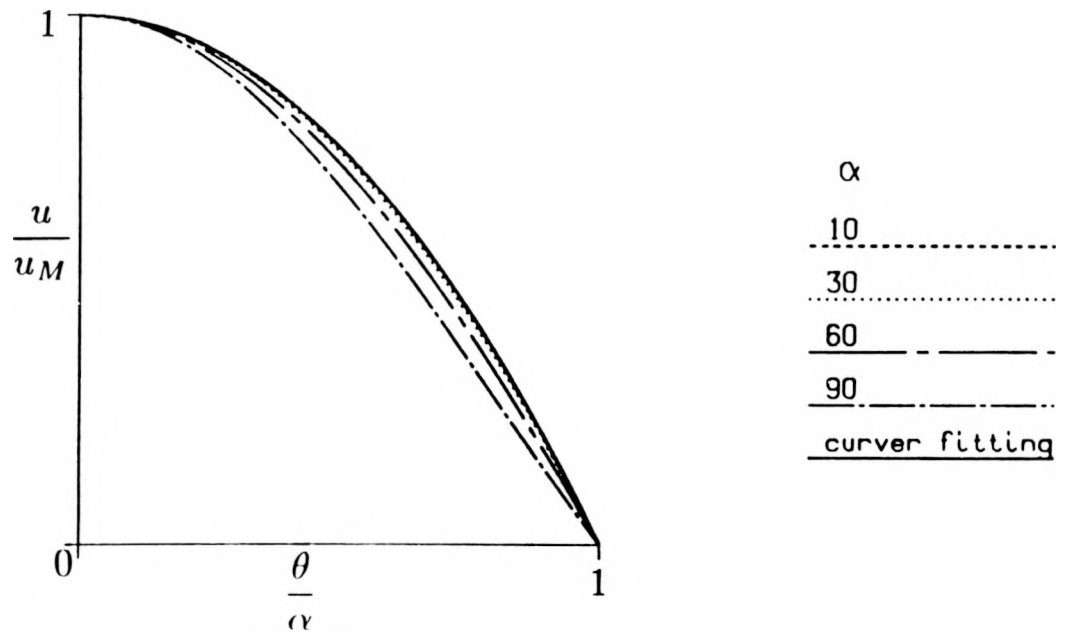
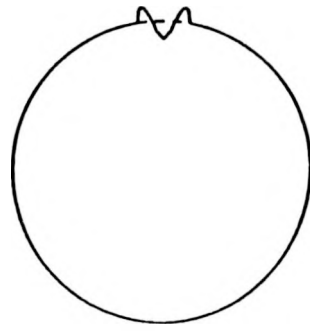
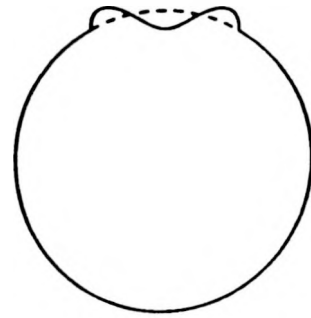


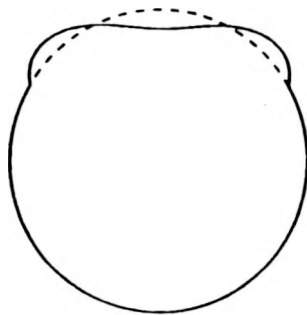
Figure 7 Normalized axial displacements of a semi-infinite long pipe with a circumferential crack at the fixed end subjected to remote bending.



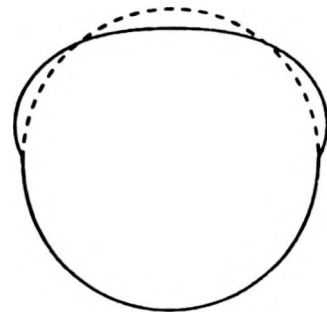
(a)



(b)



(c)



(d)

Figure 8 Normalized normal displacements of a semi-infinite long pipe with a circumferential crack at the fixed end subjected to uniform remote tension.
(a) $\alpha = 10^\circ$ (b) $\alpha = 30^\circ$ (c) $\alpha = 60^\circ$ (d) $\alpha = 90^\circ$

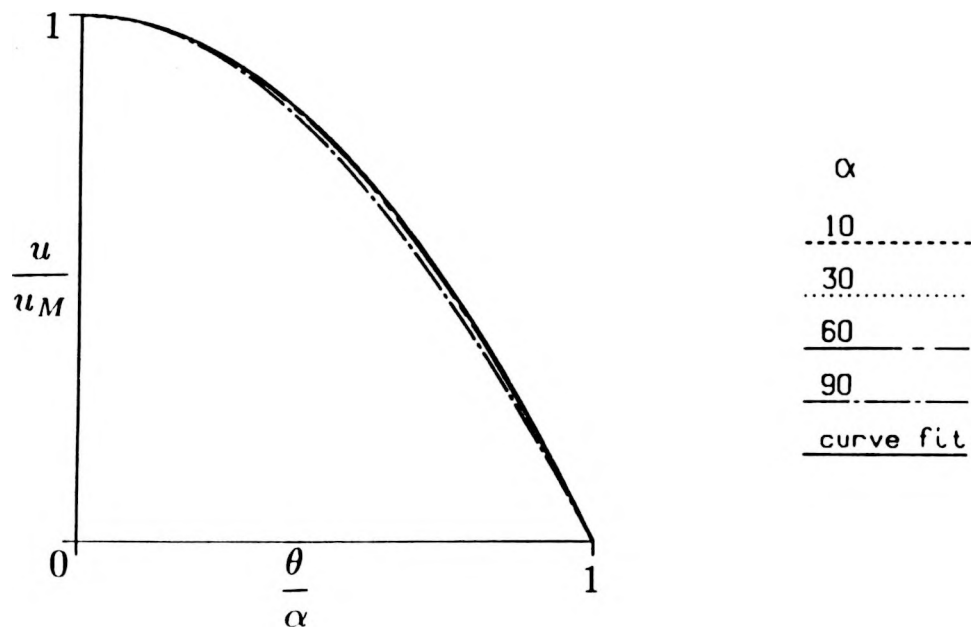


Figure 9 Normalized axial displacements of a semi-infinite long pipe with a circumferential crack at the fixed end subjected to uniform remote tension.

Crack opening profile estimation

The second order expansions of u_M and u of the solutions of Sanders and Alabi give:

$$u_M = \frac{\sqrt{2}}{2} \epsilon^{-1} \alpha^2,$$

and

$$u = \epsilon^{-1} \left(\frac{\sqrt{2}}{2} \alpha^2 - \frac{\sqrt{2}}{2} \theta^2 \right).$$

Combining two equations gives a parabolic equation:

$$\frac{u}{u_M} = 1 - \left(\frac{\theta}{\alpha} \right)^2.$$

Crack opening area estimation

The physical opening area A can be obtained by a simple integration as:

$$A = \frac{8}{3} R^2 \alpha u_M \frac{\sigma}{E}$$

for long pipes,

$$A = \frac{4}{3} R^2 \alpha u_M \frac{\sigma}{E}$$

for fixed-end long pipes.

The error of the estimation is no more than 3.5% for long circumferential cracks with the total crack angle up to 120 degrees, when compared with the shell theory results.

Conclusions

1. A simple parabolic formula for the estimation of crack opening area for long circumferential cracks with the total crack angle from (say) 45 to 120 degrees.
2. The elastic shell solutions indicate that the long pipes become taller under bending and tension and that the fixed-end pipes become taller under bending and the fixed-end pipes become shorter under tension. The taller deformed shape tends to increase the moment carrying capacity. The shorter one tends to decrease the moment carrying capacity.
3. The shorter deformed shape of the cracked pipes (usually with small cracks) under bending observed in the experiments of Wilkowski (1982) may come from the elastic-plastic pre-buckling mode of the pipes.
4. Further research on pre-buckling and post-buckling modes of cracked pipes with elastic-plastic finite deformation formulation is needed.

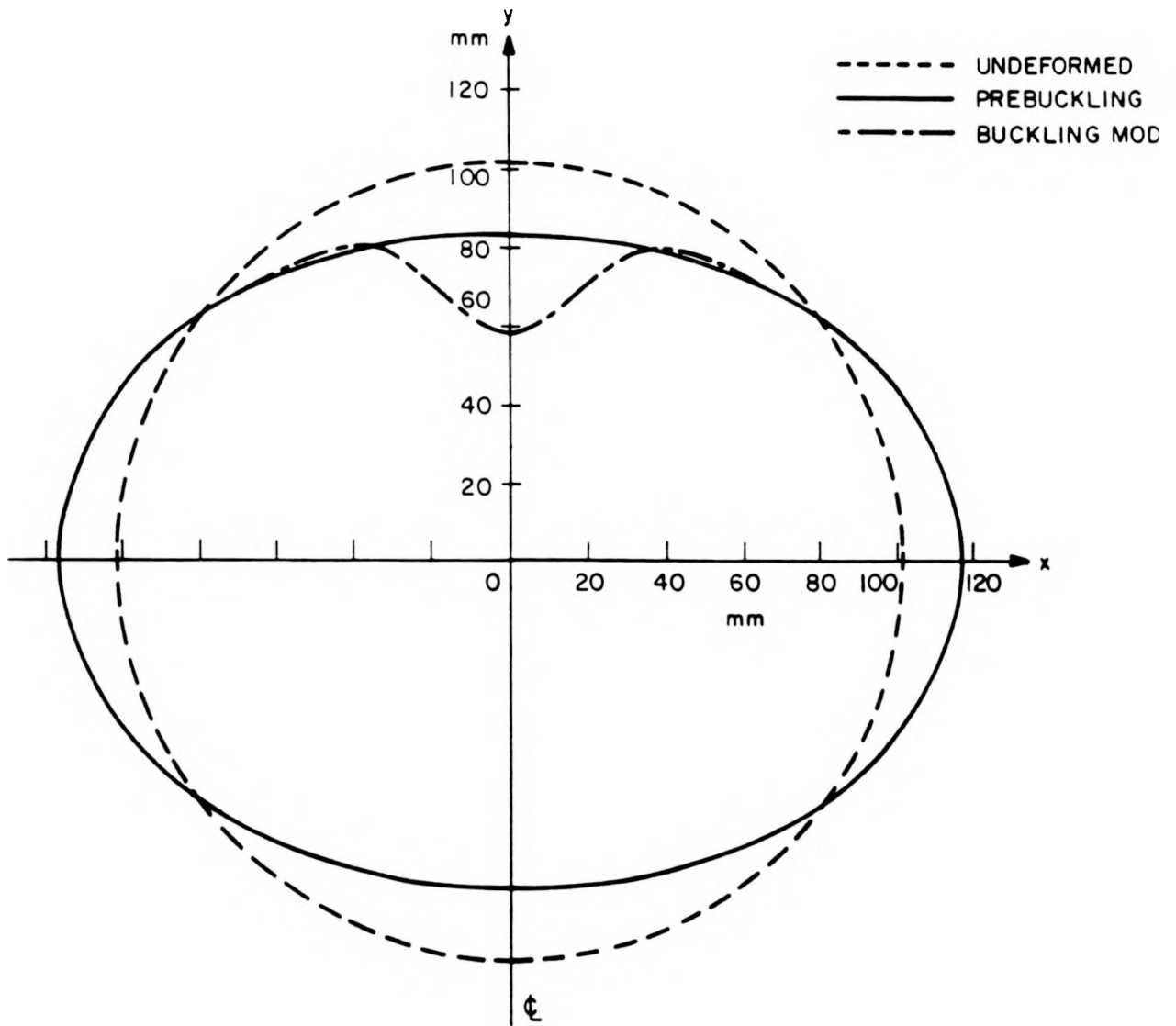
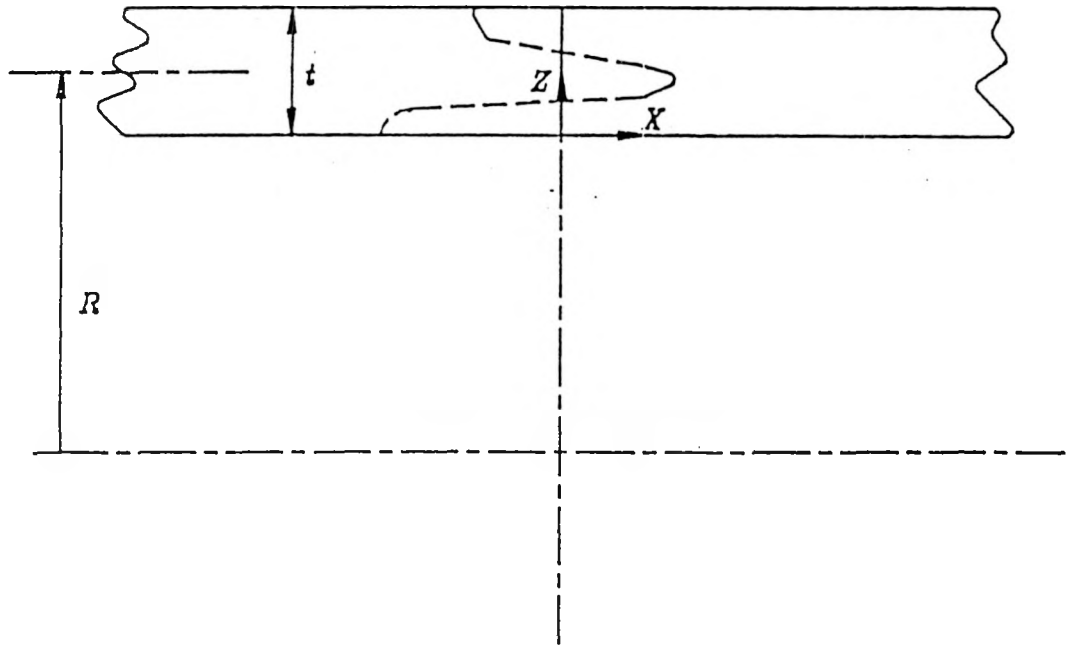


Fig. 4. Prebuckling configuration and buckling mode for a circular cross section with $a/h = 100$.

STRESS INTENSITY FACTORS of AXISYMMETRIC
CIRCUMFERENTIAL CRACK in CYLINDER

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Arbitrary $\sigma_x(X,Z)$ at $X=0$



Section of pipe under arbitrary axial stress distribution.

Cheng and Finnie's Approach (ASME. J. Mat. Engng. Tech. 1985)

$$\sigma_a(z/t) = A_0 + A_1(z/t) + A_2(z/t)^2 + A_3(z/t)^3 \quad (1)$$

$$K_I = \sqrt{\pi a} [A_0 f_0 + A_1(a/t) f_1 + A_2(a/t)^2 f_2 + A_3(a/t)^3 f_3] \quad (2)$$

$$f_0 = F_0 - \frac{3\pi G_0}{g} \quad (3a)$$

$$f_i = F_0 F_i - \frac{3\pi G_i}{g}, \quad i=1,2,3, \quad (3b)$$

for $5 \leq R_1/t \leq 20$

$$\begin{aligned}
F_0(a/t) &= 1.12 - 0.23(a/t) + 10.6(a/t)^2 \\
&\quad - 21.8(a/t)^3 + 30.4(a/t)^4, \quad a/t < 0.6 \\
&= (1+3a/t)/[2(\pi a/t)^{1/2}(1-a/t)^{3/2}], \quad a/t \geq 0.6
\end{aligned}$$

$$g = S(a/t) + \frac{\sqrt{R/t}}{[3(1-\nu^2)]^{1/4}}$$

$$\begin{aligned}
S(a/t) &= \left(\frac{a/t}{1-a/t} \right)^2 [5.93 - 19.69(a/t) + \\
&\quad 37.14(a/t)^2 - 35.84(a/t)^3 + 13.12(a/t)^4]
\end{aligned}$$

$$G_1 = \frac{f^m(a/t)}{(a/t)^1} \int_0^{a/t} (a/t)^{i+1} F_0(a/t) F_1(a/t) f^m(a/t) d(a/t)$$

$$\begin{aligned}
f^m(a/t) &= 1.122 - 1.40(a/t) + 7.33(a/t)^2 - 13.08(a/t)^3 \\
&\quad + 14.0(a/t)^4
\end{aligned}$$

$$F_1(a/t) = 0.636 - 0.365(a/t) + 0.0581(a/t)^2$$

$$F_2(a/t) = 0.5 - 0.4185(a/t) + 0.0802(a/t)^2$$

$$F_3(a/t) = 0.4244 - 0.4217(a/t) + 0.0936(a/t)^2$$

Modify

Delale and Erdogan.

$$F_0 = \sqrt{\pi\xi}(1.1216 + 6.5200\xi^2 - 12.3877\xi^4 + 89.0544\xi^6 \\ - 188.6080\xi^8 + 207.3870\xi^{10} + 32.0524\xi^{12})$$

$$f^m = \sqrt{\pi\xi}(1.1202 - 1.8872\xi + 18.0143\xi^2 - 87.3851\xi^3 \\ + 241.9124\xi^4 + 319.9402\xi^5 + 188.0105\xi^6)$$

where $\xi = (a/t)$

K EVALUATION from FEM RESULTS

STIFFNESS DERIVATIVE METHOD

J-INTEGRAL

DISPLACEMENT or STRESS EXTRAPOLATION

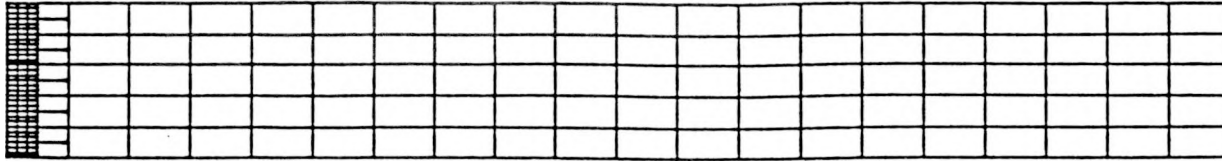
$$K_I = \frac{G}{2(1-\nu)} (4u_B - u_C) \sqrt{\frac{2\pi}{l}}$$

$$K_I = \frac{G}{2(1-\nu)} \left(\frac{8u_B - u_C}{3} \right) \sqrt{\frac{2\pi}{l}}$$

GEOMETRIC CORRECTION FACTORS

CALCULATED BY FEM, FOLLOWING
BUCHALET and BAMFORD PROCEDURE

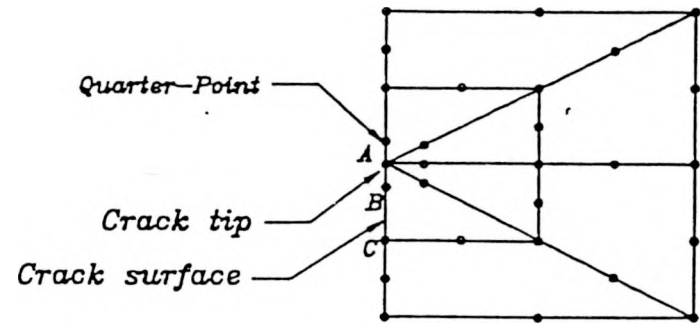
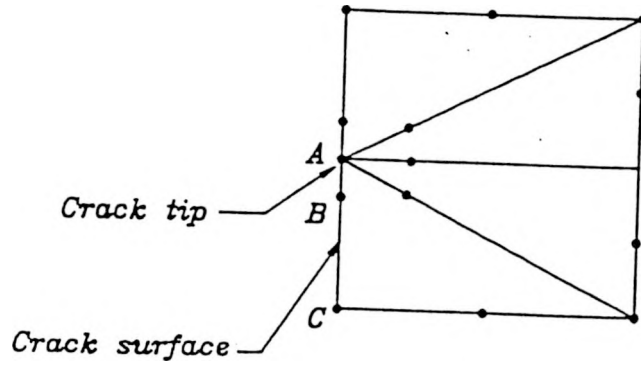
UNIFORM LOADING	F0
LINEAR LOADING	F1
QUADRATIC LOADING	F2
CUBIC LOADING	F3

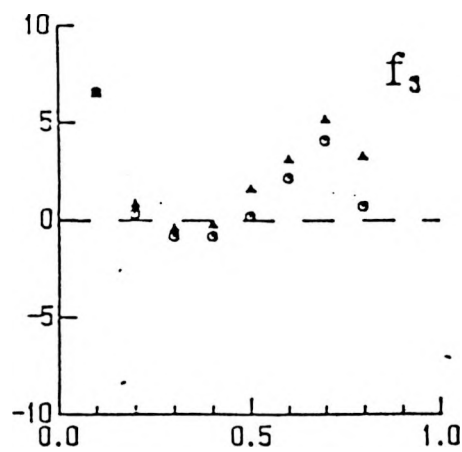
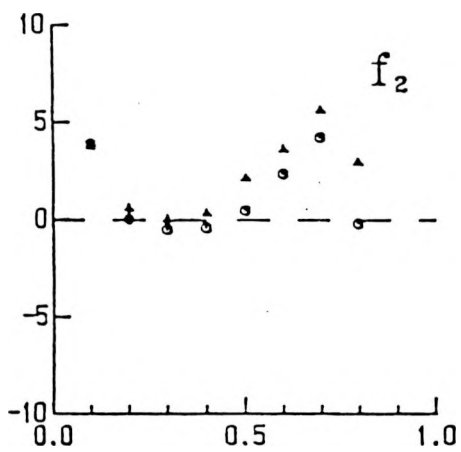
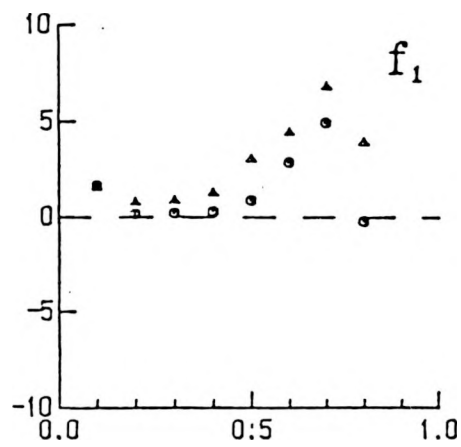
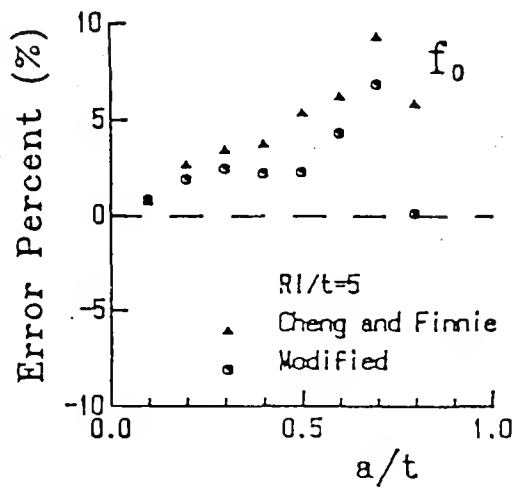


Quarter-point element mesh surrounding the crack tip

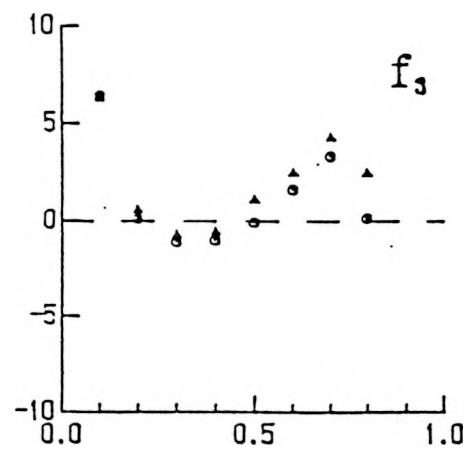
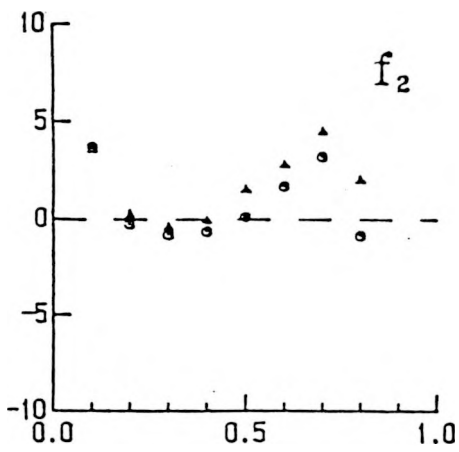
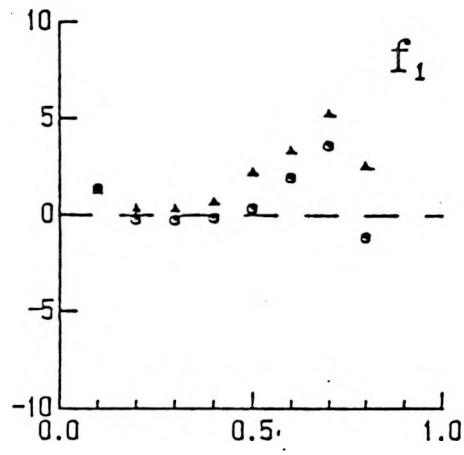
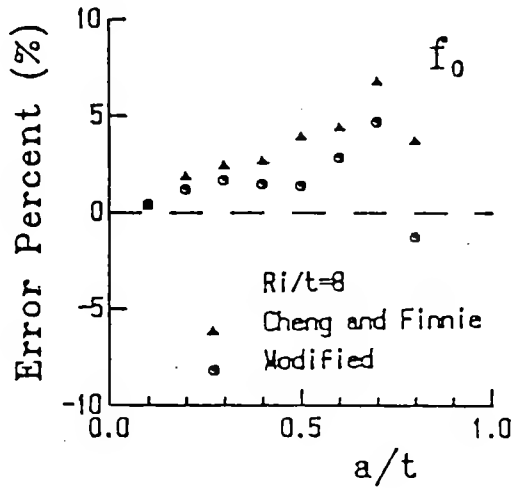
Study of the convergence

268

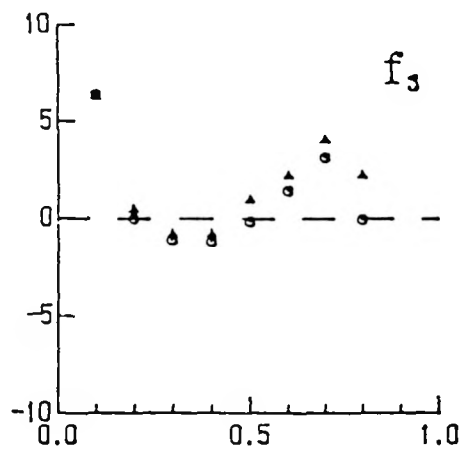
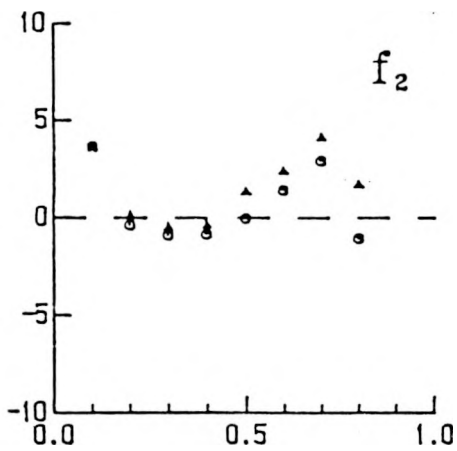
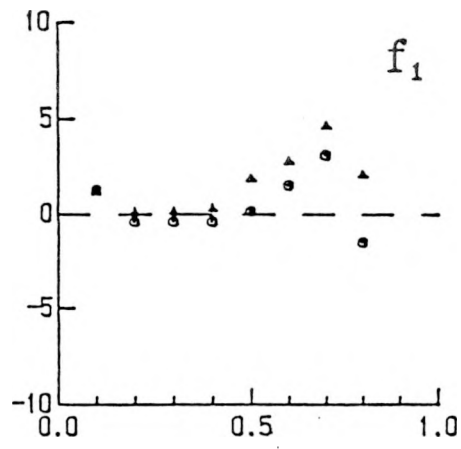
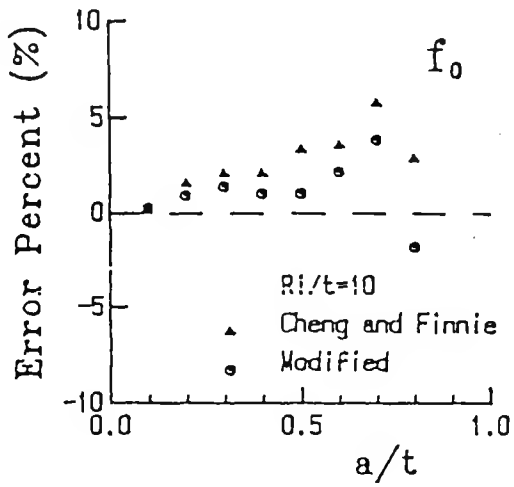




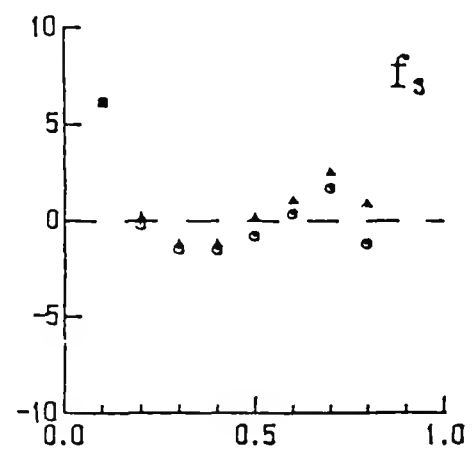
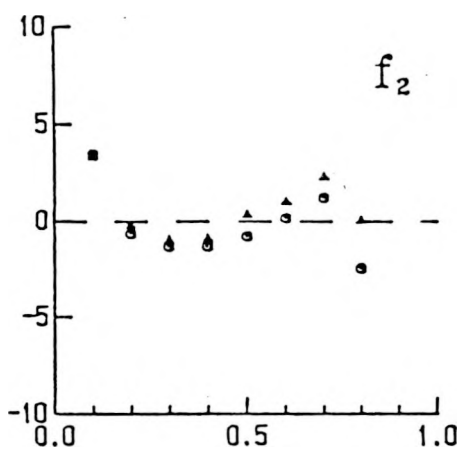
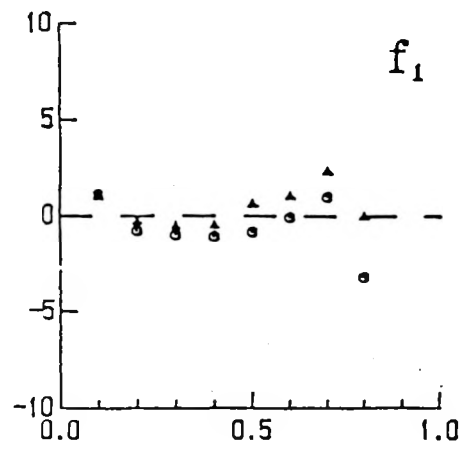
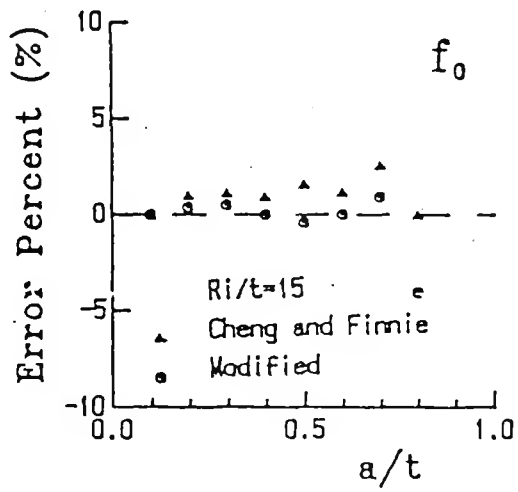
Error percent of geometric correction factors at $Ri/t=5$.



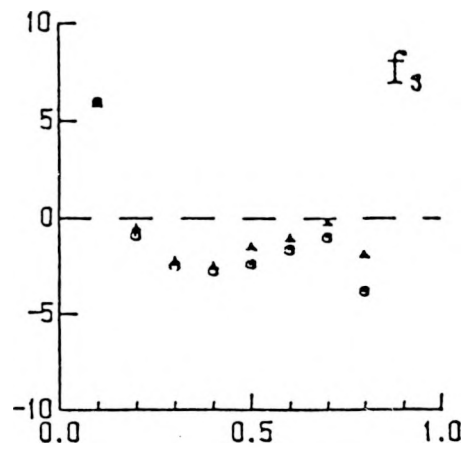
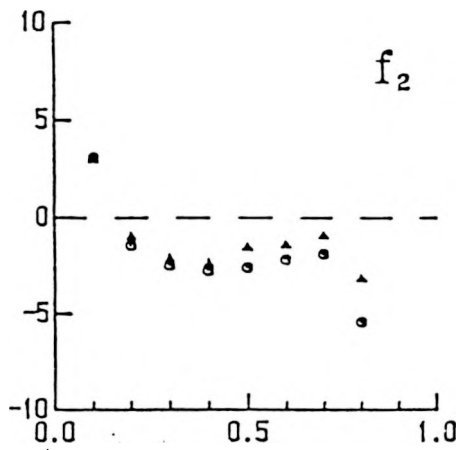
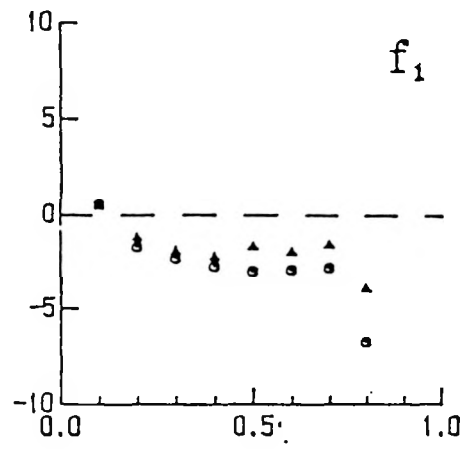
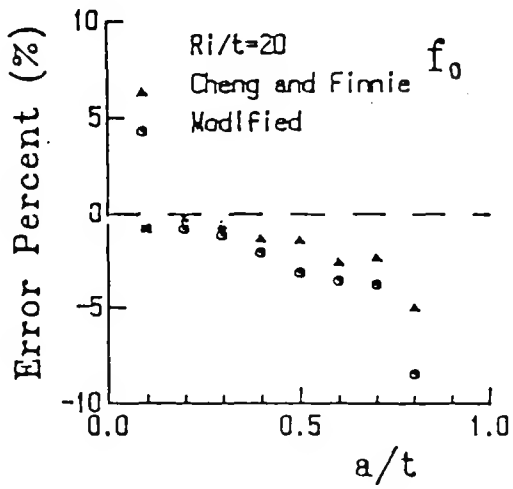
Error percent of geometric correction factors at $Ri/t=8$.



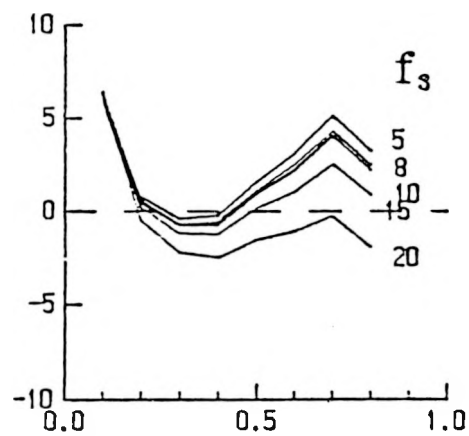
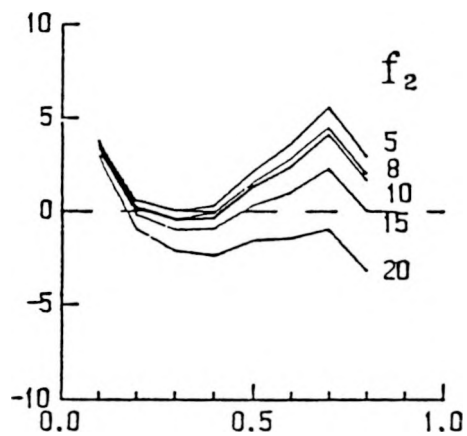
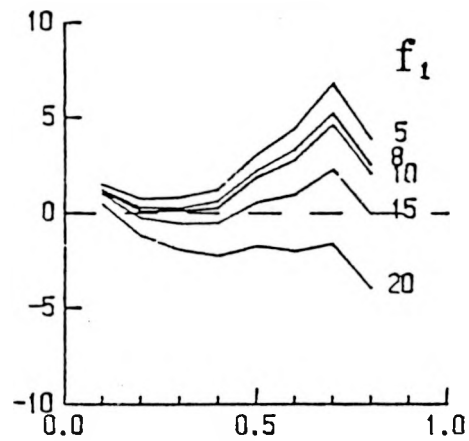
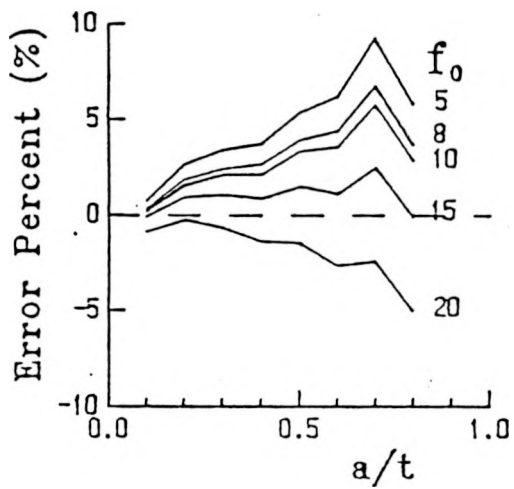
Error percent of geometric correction factors at $Ri/t=10$.



Error percent of geometric correction factors at $Ri/t=15$.



Error percent of geometric correction factors at $Ri/t=20$.



Error percentage of geometric correction factors for $Ri/t=5,8,10,15,20$

CONCLUSIONS

A K-determination technique of axisymmetric circumferential crack of cylinder, under the assumption of plane strain, is evaluated. In general, this is a very powerful technique because it can be applied to a large range of R_i/t ratio under arbitrary stress distribution.

For the series of the results of the specified R_i/t ratio, we found that the results will large than FEM results when $R_i/t < 15$.

The error are under 5 percent for $a/t < 0.6$ over all the range of $5 < R_i/t < 20$.

**Ductile Fracture Analysis by
Strain Energy Density Function**

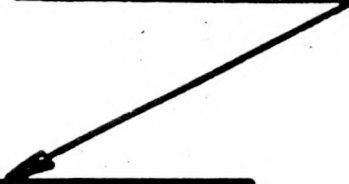
**Kuen Ting and Jinhua-Shya Yu
Institute of Nuclear Energy Research
Republic of China**

May 12, 1989

Linear Elastic Fracture Mechanics



Stress intensity factor
Energy release rate
Path independent integral
Crack opening displacement



Brittle Fracture

Ductile Fracture

?



G.C. Sih

STRAIN
ENERGY
DENSITY
FUNCTION

- THE STRAIN ENERGY DENSITY THEORY HAS BEEN EMPLOYED SUCCESSFULLY TO ANALYZE THE DUCTILE FRACTURE OF METALS WITH PRE-EXISTING CRACKS.
- STRAIN ENERGY DENSITY FUNCTION

$$\left(\frac{dW}{dV} \right) = \int_0^{\epsilon_{ij}} \sigma_{ij} d\epsilon_{ij}$$

σ_{ij} and ϵ_{ij} are STRESS AND STRAIN COMPONENTS AT A GIVEN POINT.

$$\left(\frac{dW}{dV}\right)_c$$

Critical value of strain energy density function corresponding to failure

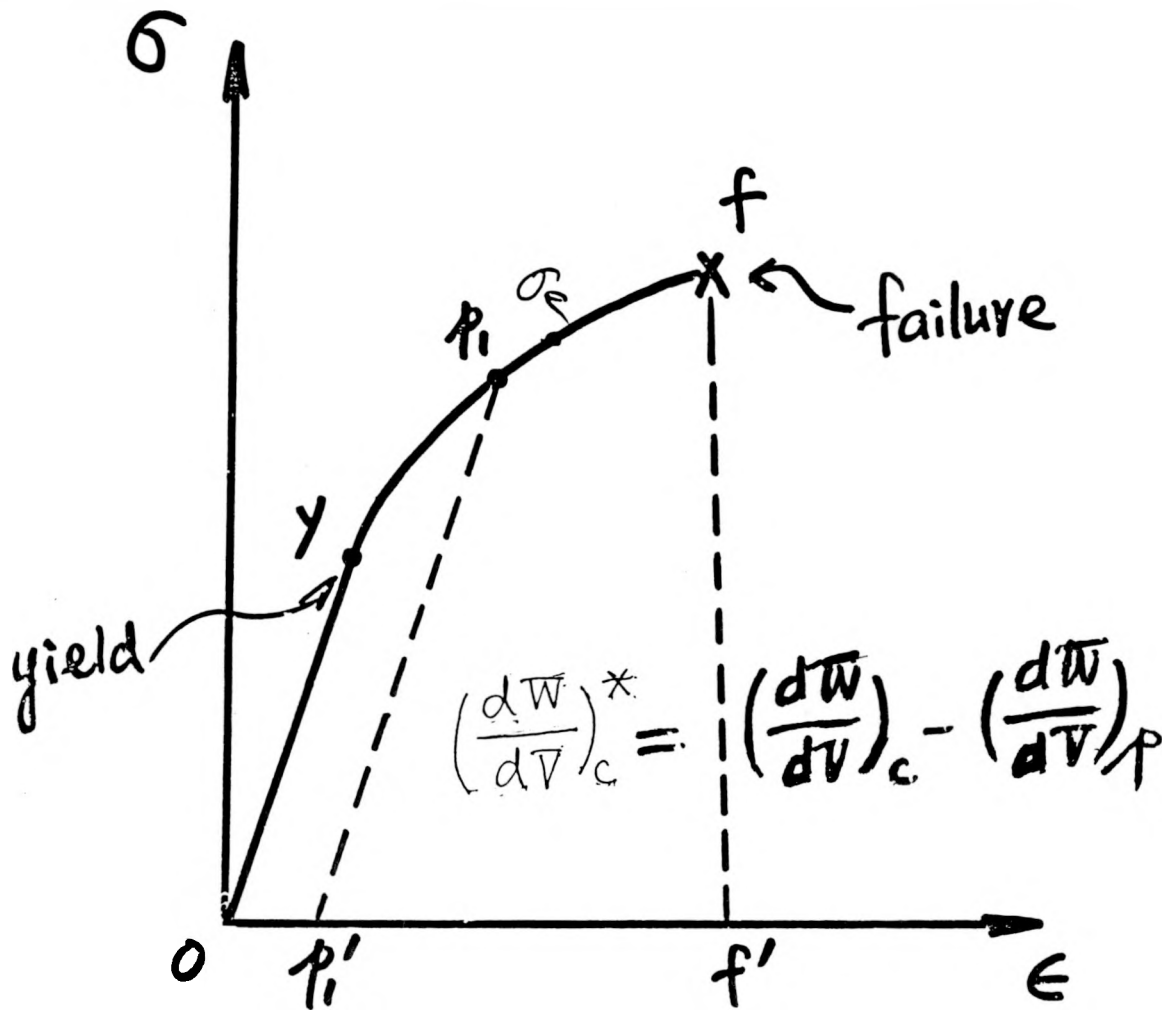
$$\left(\frac{dW}{dV}\right)_v$$

Unviable strain energy density function as the stress surpasses the yield point, permanent deformation prevails.

$$\left(\frac{dW}{dV}\right)_i$$

Crack initiation of strain energy density function

$$\left(\frac{dW}{dV}\right)_i = \left(\frac{dW}{dV}\right)_c - \left(\frac{dW}{dV}\right)_v$$

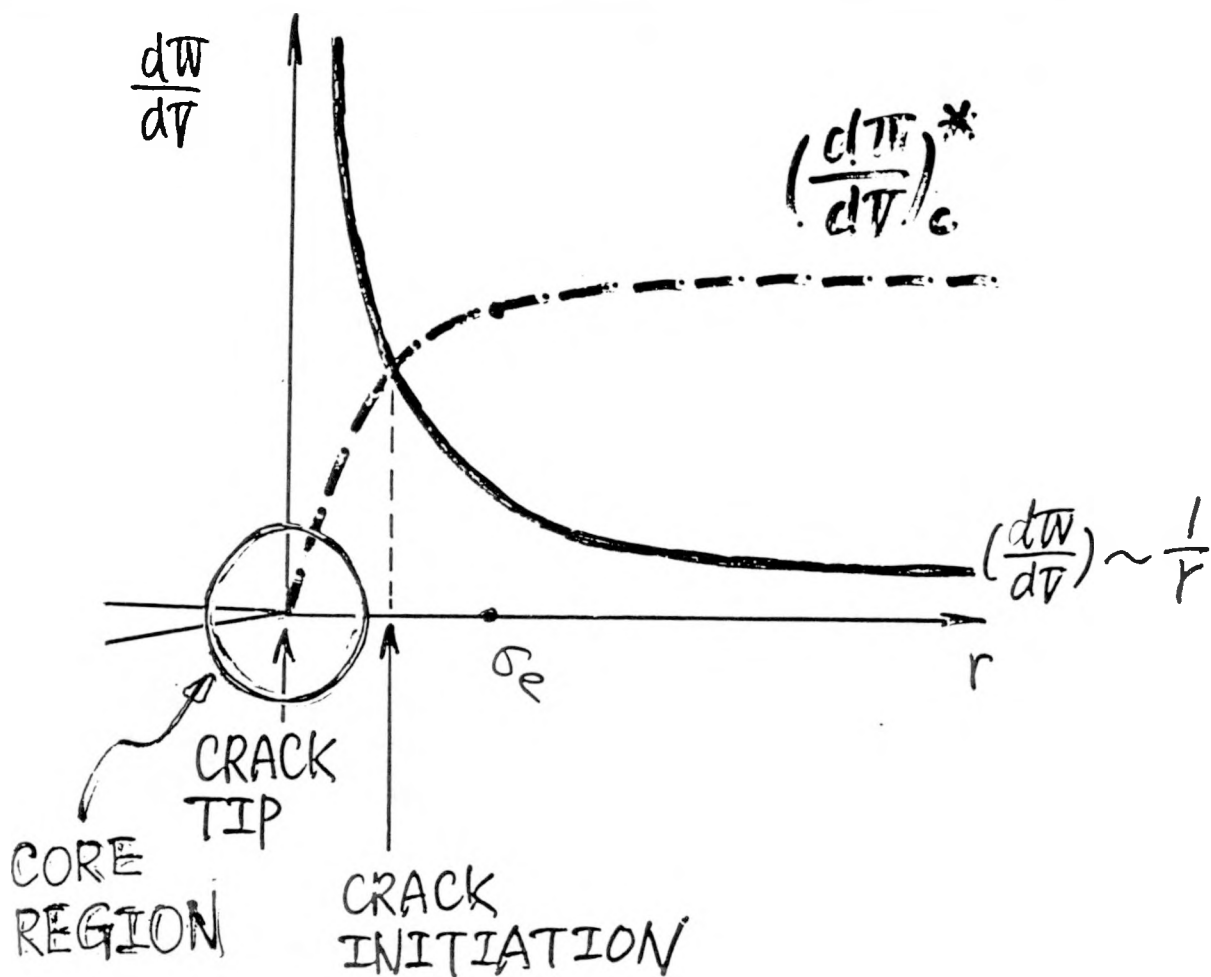


TRUE STRESS-STRAIN CURVE

$$\left(\frac{dW}{dV}\right)_c = \text{AREA OF } OYff'$$

$$\left(\frac{dW}{dV}\right)_p = \text{AREA OF } OYp_1p_1'$$

$$\left(\frac{dW}{dV}\right)_c^* = \text{AREA OF } p_1'p_1ff'$$



BASIC ASSUMPTION

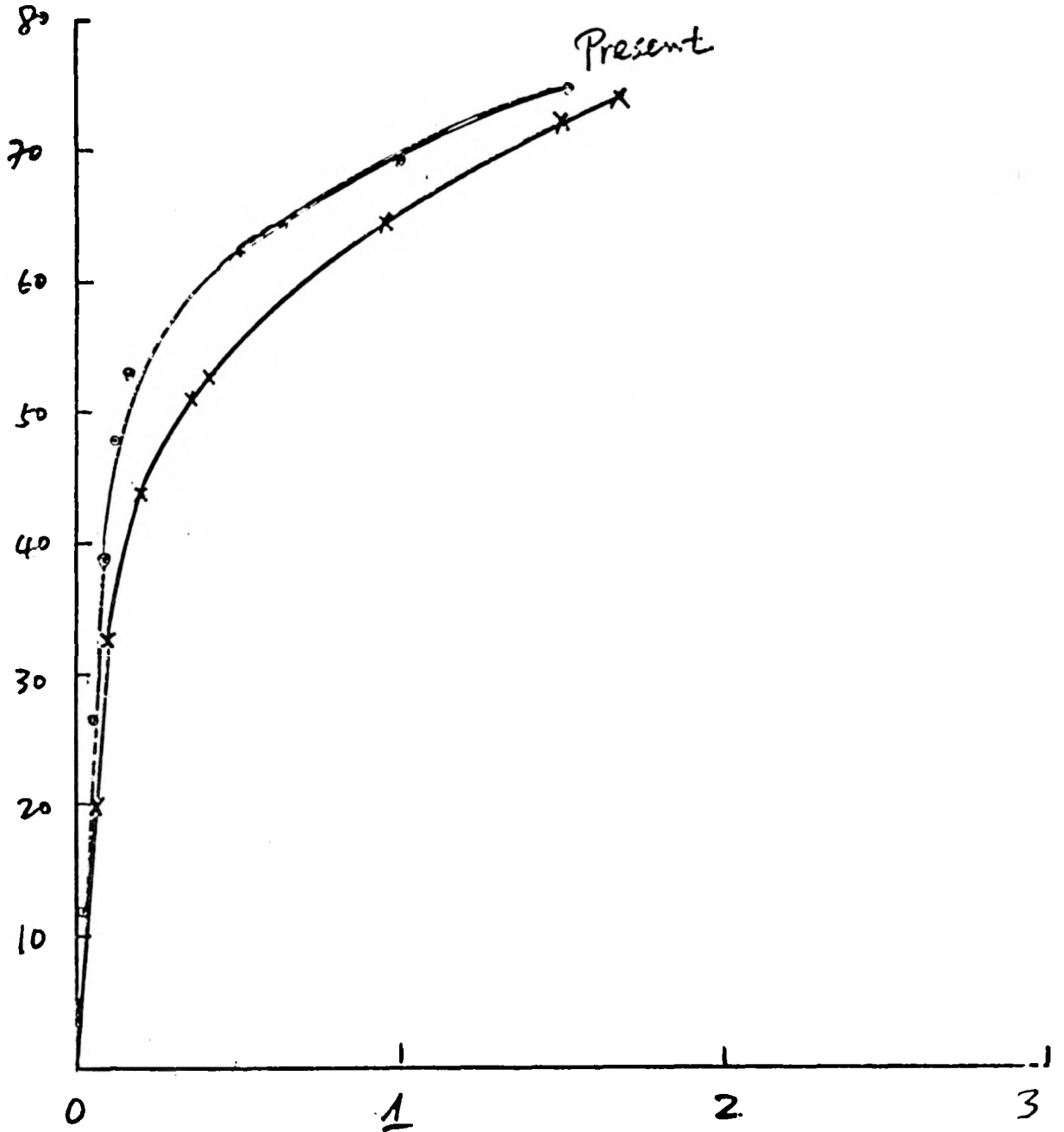
$$\left(\frac{dW}{dV}\right) = \left(\frac{dW}{dV}\right)^*_c$$

FRACTURE INITIATION

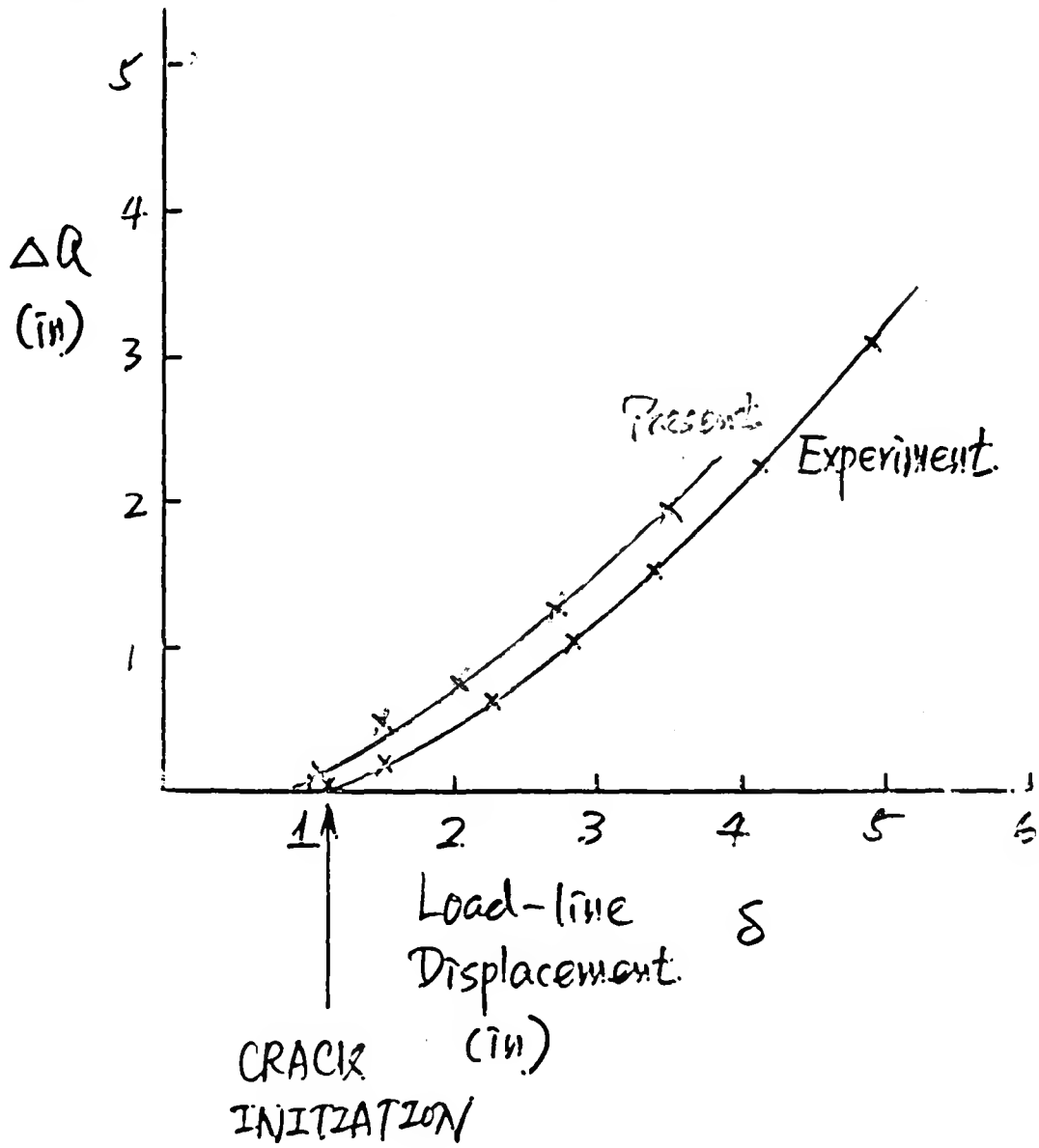
Nonlinear elastic-plastic stress analysis

- ANSYS Finite Element Program
- Incremental theory of plasticity
with von Mises flow rule
- Node release associated with crack growth
- The first Battelle/NRC Round Robin problem (NUREG/CR-4573)

P
 $(\frac{lb}{10^3})$



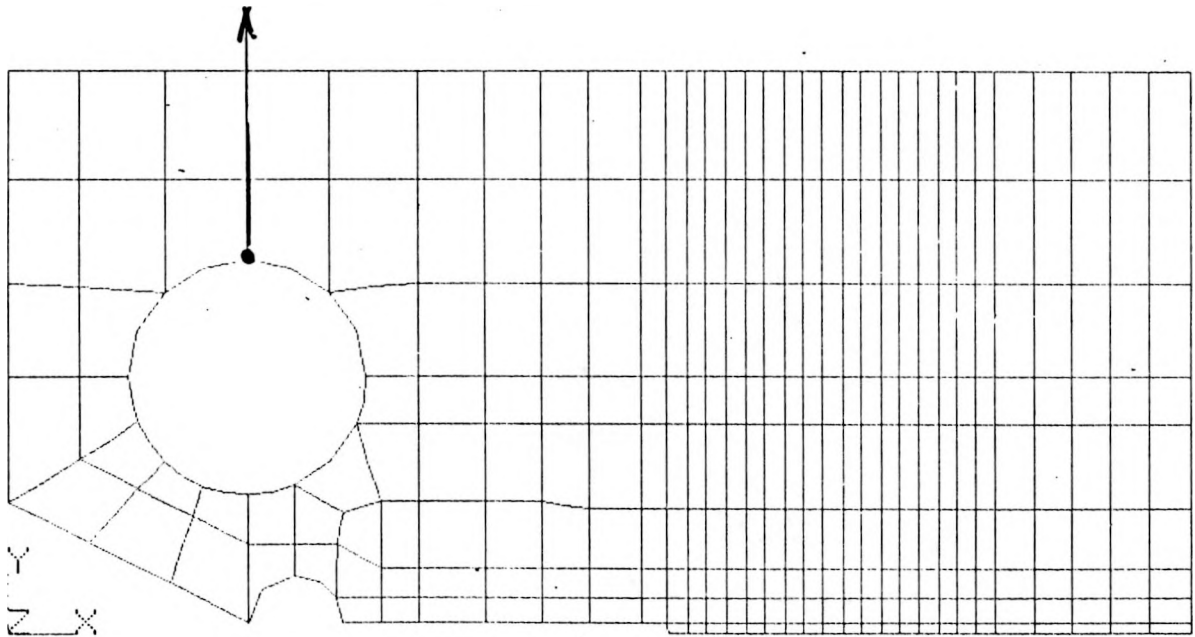
Load-line Displacement
(in)



$$\left. \begin{array}{l}
 \delta = 1.035 \text{ in} \\
 P = 67.4 \text{ lb}/10^3
 \end{array} \right\} \text{ (Experiment)}$$

$$\left. \begin{array}{l}
 \delta = 0.90 \text{ in} \\
 P = 69 \text{ lb}/10^3
 \end{array} \right\} \text{ (Prediction)}$$

PRESCRIBED
DISPLACEMENTS



initial
crack
tip

PLATE FRACTURE

CONCLUSIONS

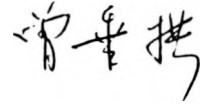
- * The strain energy density function can be used to predict the stable crack growth in ductile fracture analysis.

- * This approach is easily extended to the 3D ductile fracture analysis.

- * The available data are very simple, only $\sigma - \epsilon$ curve is necessary.

Ductile Fracture Experiment on Through-wall Crack Piping

C.G. Tseng



Department of Mechanical Engineering
National Taiwan Institute of Technology

ABSTRACT

The main purpose of this project is to study the ductile fracture behaviors of a Stainless 304 through-wall cracked pipe subjected bending load. The crack initiation J_{IC} and fracture instability-tearing modulus are searched during the experiment. Constant displacement condition is employed during a four-point bending test of a cracked pipe. Load versus displacement, load versus crack opening displacement, as well as crack opening displacement versus crack growth are plotted simultaneously during the test. While crack growth is monitored by a "Kraak Gage". The crack initiation J_{IC} and tearing modulus are then determined from these plots. In order to get a more convinced result, fatigued precrack instead of machined notch is used to simulate the true crack. The details of the experiment are to be discussed.

1. Introduction

The main purpose of this project is to study the ductile fracture behaviors of a stainless 304 through-wall cracked pipe subjecting bending load. As we know stainless 304 pipes are used widely in nuclear power plant, therefore, to study the fracture behavior of this kind of pipes is always concerned by many investigators. Once a cracked pipe was found, people might have two questions to ask : will the crack continue growth ? When the pipe will collapse? On other words: what condition will make the crack growth initiate? and what condition will make the fracture instability occur ? Since stainless 304 has non-negligible plastic deformation before it collapses, the conventional linear fracture mechanics fail to describe its fracture behavior. Hence, there is a need for a plastic fracture mechanics approach. One of the elastic-plastic fracture mechanics theory attract many investigators is J-integral theory. In the theory, when the applied J reach the material property J_{mat} the crack initiates and when the applied dJ/da reaches the material property dJ/da the crack instability fracture occurs. There are several papers dealt with the J-integral study of crack pipes. Especially the one from Battelle Laboratory, an experimental method was proposed in their paper. Basically, this project followed the method they proposed, except the techniques to measure the crack growth and the specimen been fatigue precracked.

In the experiment work , stainless 304 schdule 20, diameter $1\frac{1}{2}$ inch through-wall cracked pipes were tested by 4-point bending. The initiation J and the occurence of fracture instability were investigated.

2. J-Resistance curve and tearing Modulus Approach

In literature paper[1,2], the cracked pipe will have crack growth initiation when applied J equals J_{Ic} . Once the crack grow the applied J is equal to the material resistance J_{matl} which is a function of the crack growth Δa . After some amount of stable crack growth, the fracture instability may occur when $\frac{dJ_{app}}{da} > \frac{dJ_{matl}}{da}$. The J-integral can be determined by a four point bending test. Figure 1. shows the scheme of the experimental set up. The J-integral can be separate into two parts :

$$J = J_{el} + J_{pl}$$

where, in some cases, J_{el} was found much smaller than J_{pl} , and can be neglected. And J_{pl} can be obtained through formula :

$$J_{pl} = \frac{1}{Rt} \int_0^P \left(\frac{\partial \delta}{\partial \Phi} \right) dp$$

and this is equivalent to

$$J_{pl} = \beta \int_0^{\delta_{pl}} P d\delta_{pl}$$

where β is a function of the crack angle Φ , and the deflection δ_{pl} is the plastic part deflection of the load point which can be obtained by subtracting δ_{el} from the total deflection. The above equation is valid until the crack initiation. If the instance of crack initiation can be determined, then the initiation J can be calculated by the above formula. Once the crack growth, the crack length increase, the J-integral can be written as

$$J_{pl} = \beta \int_0^{\delta_{pl}} P d\delta_{pl} + \int_{\Phi_0}^{\Phi} \gamma J d\Phi$$

where Φ is the crack angle and γ is function of Φ .

After the J- Δa curve can be plotted the slope of dJ/da can be obtained from the curve, and this is the material resistance. The applied dJ/da under displacement was given as :

$$\left(\frac{\partial J}{\partial a}\right)_{\delta} = 2t(\beta P) C_{elP} \left(1 + \frac{C_s}{C_{elP}}\right) + \frac{2\gamma}{R} J$$

where C_{elP} represents the elastic compliance of an uncracked pipe, and C_s represents the compliance of the spring. When this applied dJ/da greater than the material dJ/da , fracture instability occurs.

3. Description of the Specimen

In this project, the specimen was a stainless 304 schedule 20 pipe, nominal diameter $1\frac{1}{2}$ " , thickness 3 mm, length $Z=440$ mm , $L=220$ mm . To simulate the behavior of the crack (crack with no width) fatigue load was applied to the machine - notched pipe to form a precrack 0.6 mm , after the fatigued precrack , the total crack length was 38.2 mm . This was done by applying sinusoidal load with $P_{max}=750$ kg and $P_{min}=50$ kg under frequency 10 Hz, running 26000 cycles .

4. The experiment process

In order to perform a displacement control test, we used a spring which compliance was about 21 times of the compliance of the pipe with no crack. The loading machine was a Japan made Shimadzu model 48-25, 20 ton dynamic testing machine, loading rate was 1.2mm/min. Displacement transducer was used to measure the displacement of the load point δ . And the crack opening displacement was measured by a clip gage. An electric potential technique was proposed by reference[3] constant current was supplied to the pipe, if there is crack growth, then the electric potential between two ends of the pipe will change. Therefore, by measuring the electric potential between two ends of the pipe, we can tell how the crack

grows. In fact, when crack grows, the change of the electric potential is very small. In other words, the change of this electric potential may not be sensitive to a small amount of crack growth. To overcome this problem, we applied constant current and measure the electric potential between two sides of the crack center (same place as we measured COD).

After the calibration, we found a good linear relationship between this electric potential change and crack growth (Fig 2). This electric potential change was big enough to read out easily by conventional instruments.

During the bending test, load versus load point displacement, load versus crack opening displacement, and crack opening displacement versus electric potential were plotted. The data were read in computer through a digitizer, then the corresponding calculations were carried out using the computer.

5. Results and Discussions

Figure 3 shows the load versus load point displacement. The area under this curve is related to the J integral. In this figure, we can see the elastic displacement is small compared with the total displacement when instability occurs. The maximum load P that can be applied to the specimen was found in this figure. The instability fracture occurred right after this maximum load P . According to ref [2], instability fracture load was 97% of the maximum load. Since it is not easy to define which point is the onset of instability, we simply use maximum load P as the instability load.

Figure 4 shows the load versus COD, again, in the figure we see the elastic displacement is very small. When the load reaches P , which is the point we define as onset of instability, the COD value, at this moment, is about 9mm.

Figure 5 shows the COD versus ΔV (electric potential change). If the crack doesn't grow, but the crack tip is subject to plastic deformation, there will be a linear relationship between COD and ΔV . This is presented as the blunting line in the figure. When

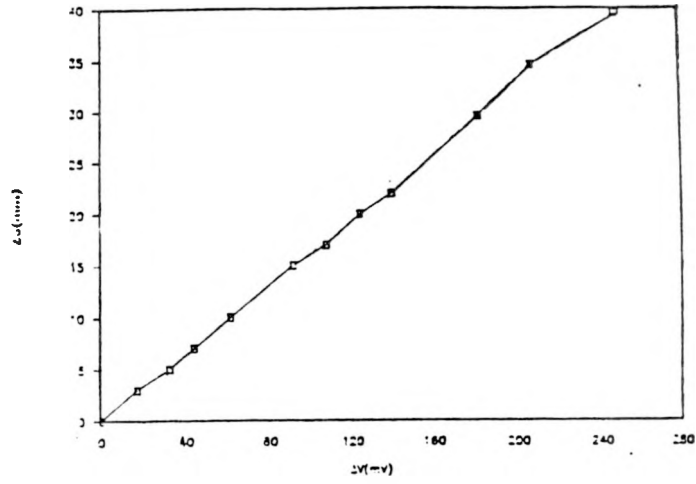


Fig.2 Calibration curve of the relationship between crack length and electric potential drop

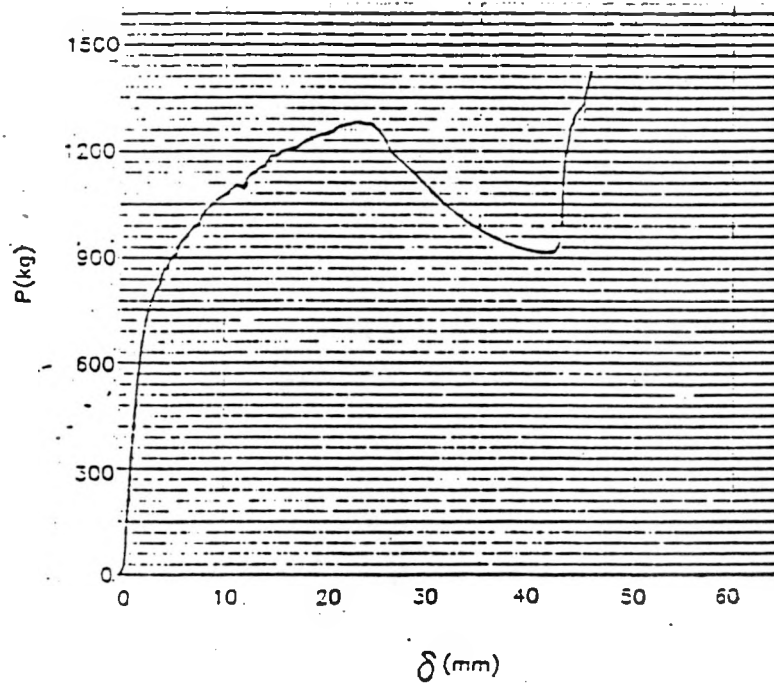


Fig.3 Load versus load point displacement

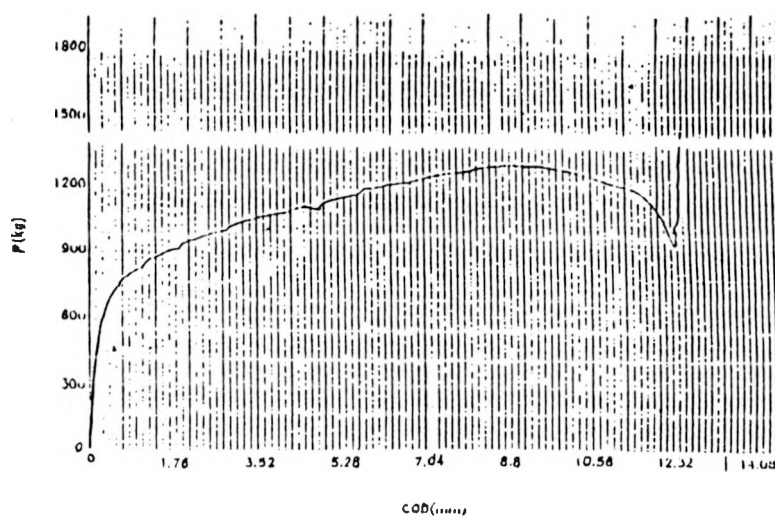


Fig. 4 Load versus crack opening displacement

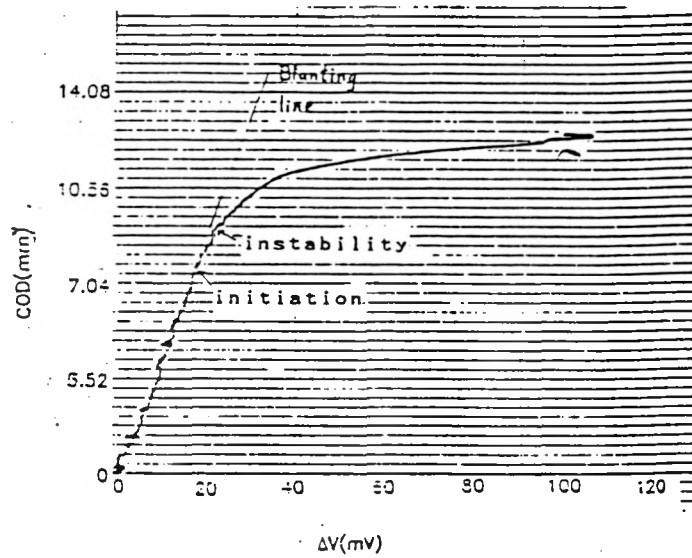


Fig. 5 Crack opening displacement versus electric potential drop

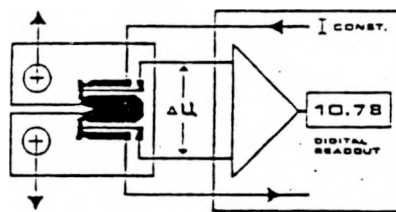


Fig. 6 KRAK-GAGE/FRACTOMAT indirect potential measuring method

there is crack growth, the COD vs. ΔV curve will leave this blunting line. By using this theory, we can determine the crack initiation point. Trace this point to $P-\delta$ curve, we are able to calculate J_{ic} as 1.683MN/m. This value is greater than the one from ref[2] 1.051MN/m. This greater value may result from the thickness effect, since the thickness of this pipe was thinner than that in ref[2]. As we know, the thinner the specimen is the greater the resistance will be.

In the test, before the instability occurred, the clip gage had reached its maximum expansion and fell down. Therefore, we could not use COD data to determine the instability point, as the consequence, we used maximum load point as the instability point. At this point Δa is equal to 0.81mm and J is equal to 1.781MN/m, applied dJ/da is equal to 127MN/m, while this is very closed to material $dJ/da = 121$ MN/m this may confirm the tearing modulus theory.

Crack growth amount plays a very important role in determining the J and instability. A convenient and reliable technique to measure the crack growth is using the commercial "KraK Gage", which is a thin layer of metal film, (Fig 6); that can be bonded ahead of the crack tip. When the crack grows, the resistance (or the electric potential) between two terminals will change. This resistance change can be related to the crack growth. We tried this method, and got a very good calibration curve that showed there was a linear relationship between resistance change and the crack growth. But when we applied this KraK Gage to the stainless 304 pipe specimen, we found that KraK Gage fractured before the specimen crack grows. That meant KraK Gage cannot suffer the same plastic deformation as stainless 304, therefore it broke earlier than the specimen. Though "KraK Gage" cannot be used

in this project, it maybe a good senser to measure crack growth for some other materials.

6. Conclusion

The method proposed by ref[1],[2] can be used to measure the J and instability tearing modulus of cracked pipes. The fatigued precrack simulates true crack better than machined notch. The electric potential change between two sides of the crack center can be related to the crack growth. Using the techniques mentioned above, one is able to obtain more reliable J and instability fracture point.

Reference

1. Zahoor, A., Kanninen, M.F., "A Plastic Fracture Mechanics Prediction of Fracture Instability In a Circumferentially Cracked Pipe In Bending — Part I: J-integral Analysis "ASME Journal of Pressure Vessel Technology, Vol.103, Nov.1981 ,pp.352-357
2. Wilkowski, G.M., Zahoor, A., and Kanninen, M.F., "A Plastic Fracture Mechanics Prediction of Fracture Instability In a Circumferentially Cracked Pipe in Bending — Part II: Experimental Verification on Type 304 Stainless Steel Pipe "ASME Journal of Pressure Vessel Technology, Vol.103, Nov.1981, pp.359-365
3. Loews, J.M, and Fearnough, G.D., "The Dectection of Slow Crack Growth in Crack Opening Displacement Specimens Using an Electrical Potential Method "Eng. Fracture Mechanics, Vol.3, 1971, pp.103-108
4. Paul C. Pans and Brain R Hayden "A New System for Fatigue Crack Growth Measurement and Control "Presented at the ASTM Symposium on Fatigue Crack Growth, Pittsburgh, Octorber 1979

OVERVIEW
OF CHINSHAN NUCLEAR POWER STATION
RECIRCULATION PIPE REPAIR AND REPLACEMENT

Shih-Chang Cheng
Taiwan Power Company

May 12, 1989

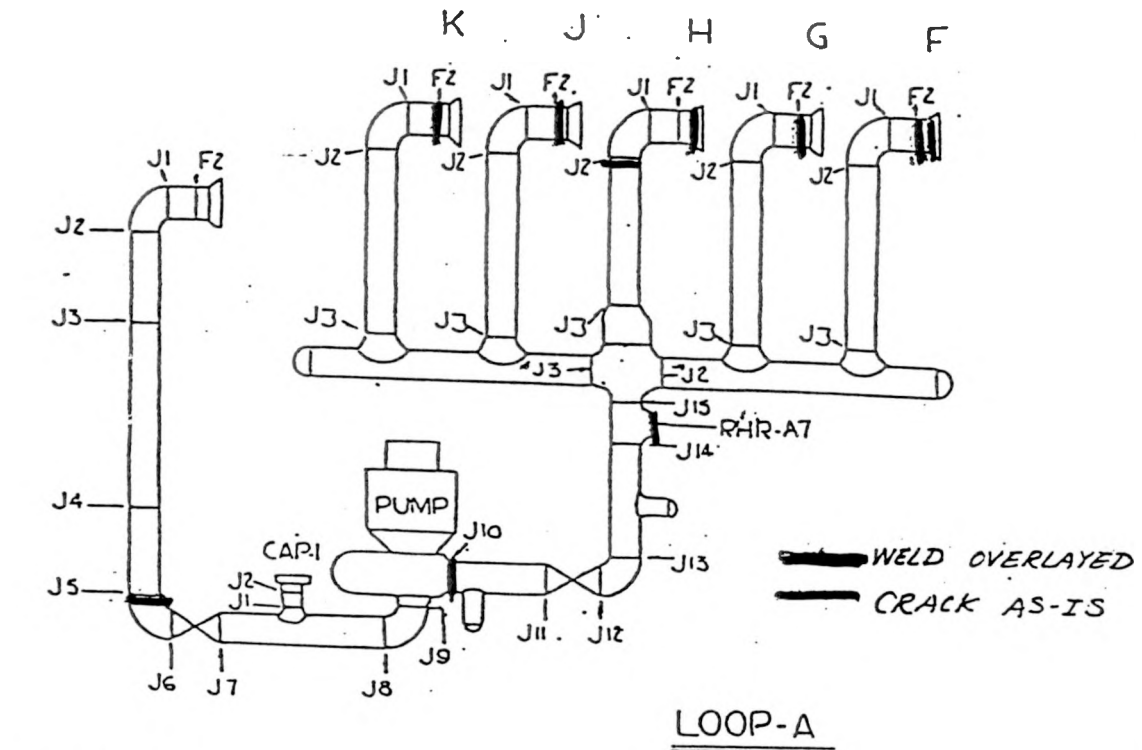
OVERVIEW OF CHINSHAN NUCLEAR POWER STATION RECIRCULATION PIPE REPAIR AND REPLACEMENT

1. Introduction
2. Objectives
3. Scope of Replacement Work
4. Preparation for the Work
5. Project Work Division
6. Design Enhancement
7. Material
8. Welding Process
9. Radiation Exposure Control
10. Safe-End Replacement
11. N2E-F1 Weld Repair & Replacement
12. Major Problem and Resolve Methods

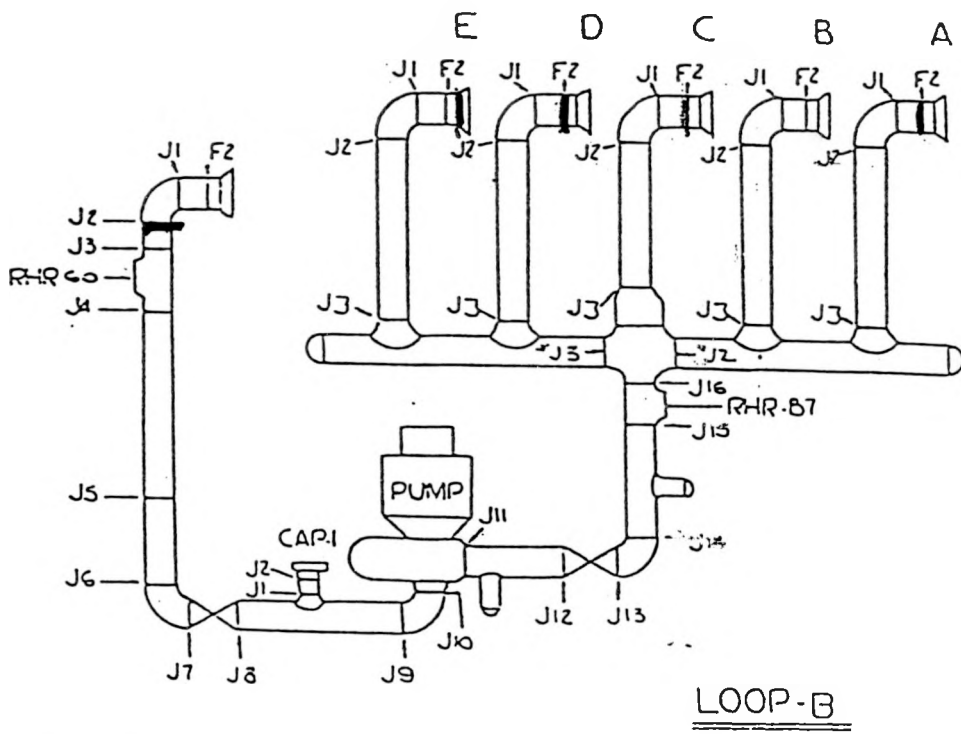
Introduction

- Chinshan NPS Has 2 Units 636 MWe GE BWR-4 Reactors with Mark I Containments
- Commercial Operation
 - Unit #1 - December 1978
 - Unit #2 - July 1979
- First IGSCC Found
 - Recirculation Pipe - 1986 (EOC-7)
 - RWCU Pipe - 1984 (EOC-5)
 - RHR Pipe - 1985 (EOC-5)
- RWCU & RHR Pipe Replaced to 316L Stainless Steel
 - Unit #1 - August 1986 (EOC-7)
 - Unit #2 - April 1986 (EOC-6)
- IHSI Performed for Recirc. Loop
 - Unit #1 - December 1983 (EOC-5)
 - Unit #2 - October 1983 (EOC-4)
- Most Indications Were Found on Riser & Safe-End Area
 - Unit #1 - 11 of 17 (65%)
 - Unit #2 - 25 of 26 (96%)

TABLE V



NOTE: * NOT EXAMINED

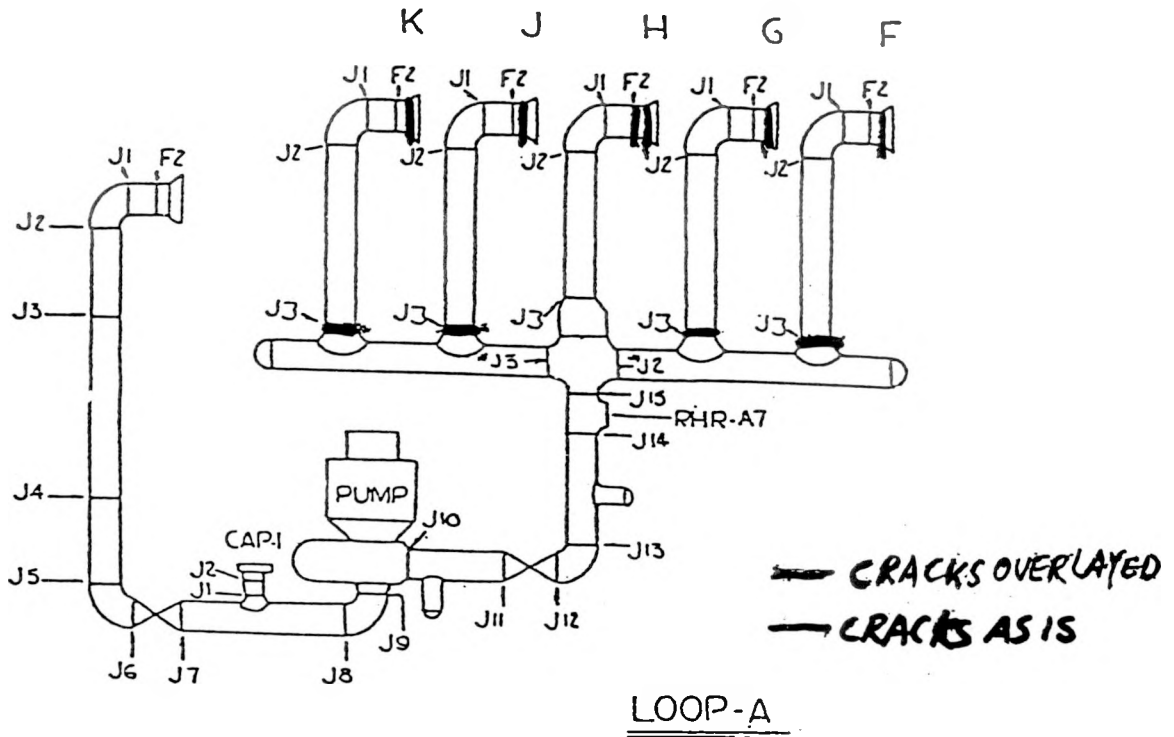


NOTE: * Not Examined

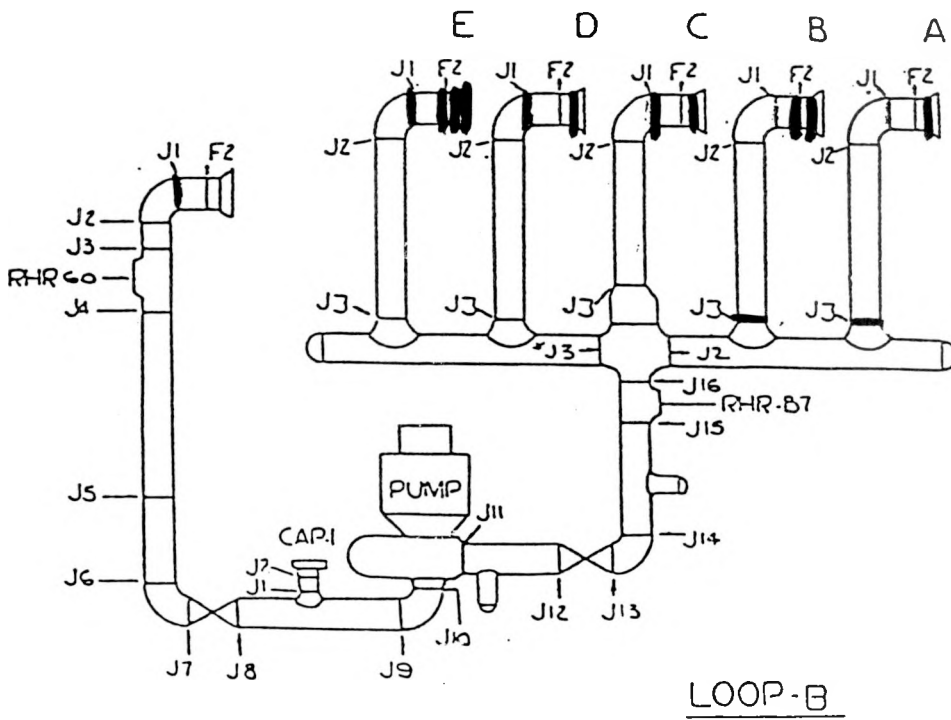
FIGURE 1

WELD IDENTIFICATION/LOCATION

TABLE V



NOTE: * NOT EXAMINED



Note: *Not Examined

FIGURE 1

Scope of Replacement Work

- Replace
All 10 In. Wrought S.S. Riser
All Recirculation Inlet Safe-End
- Repair N2E-F1 Nozzle
- Repair Any Nozzle or Butter if Required

Preparation for Replacement

- Material Procurement began in September 1986 (Unit #1 EOC-7)
- Decision Made in Mid.1987 (Unit #2 EOC-8)
- Taipower Task Force Group Established in September 1987
- Site Survey Performed During 1987.10 to 1987.12
- Installation Contractor Selected in 1988.5

Project Work Division

- General Electric Was Selected to Provide Hardware, Analysis & Construction Work
- Material Research Lab. (MRL) Provide Technical Support in Welding and Material Area
- EPRI NDE Center Provided Technical Support in N2E Nozzle Repair
- Taipower NDE Group Performed Pre-weld and Post-weld NDE Examination
- Taipower NED and NOD Coordinate with AEC for Work Quality and Adequacy Review

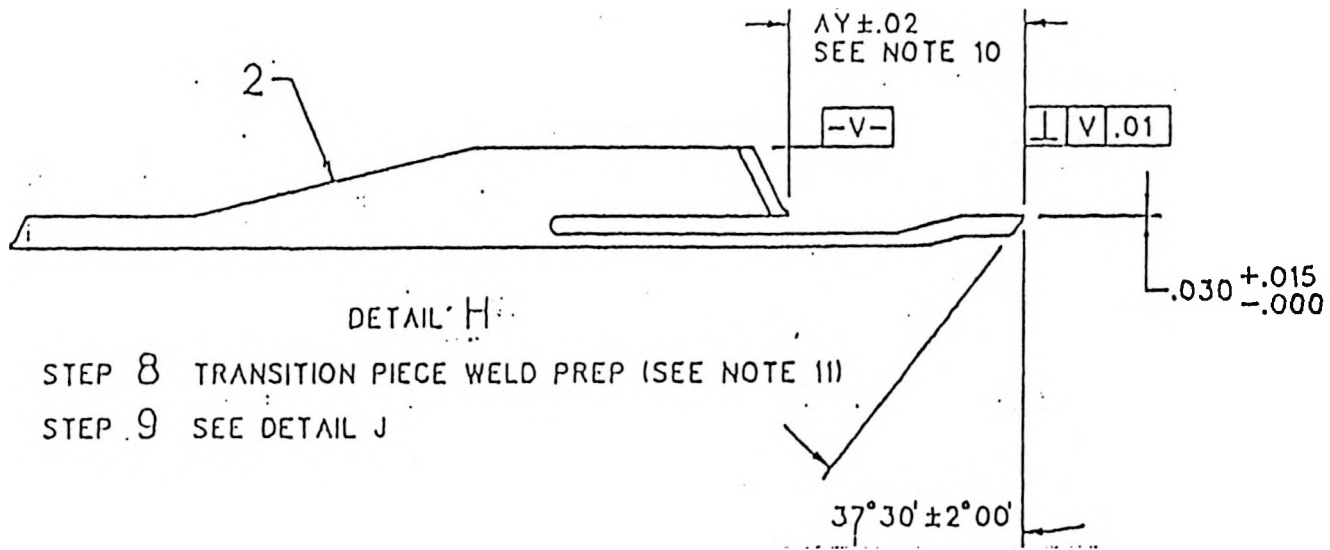
Design Enhancement

- Seamless Pipe - SS 316 Nuclear Grade
- Induction Bend Pipe Spool to Reduce Welds
* 20 Welds Eliminated Totally
- Tuning Fork Type Safe-End to Eliminate
Crevice

DETAIL R
FOR CHINSHAN 2 NOZZLE N2E ONLY

STEP 3A REMOVAL OF INDICATIONS

STEP 4 SEE DETAIL D (SHEET 1 ZONE D-9)



DETAIL H

STEP 8 TRANSITION PIECE WELD PREP (SEE NOTE 11)

STEP 9 SEE DETAIL J

Material

316 NG Stainless Steel
for Riser And Safe-End

Element -----	Weight % -----
Carbon	0.020 Max.
Nitrogen	0.06 - 0.12
Phosphorous	0.030 Max.
Sulfur	0.005 Max.
Molybdenum	2.0 -3.0
Manganese	2.0 Max.
Nickel	10 -14
Chromium	16 -18
Copper	0.5 Max.
Cobalt	0.25 Max.
Titanium	0.05 Max.
Boron	0.001 Max.
Aluminum	0.04 Max.
V	0.1 Max.
Ta & Nb	0.05 Max.
Bi+Sn+As+	
Sb+Pb+Se	0.02 Max.

Welding Process

- A. Manual, Machine or Automatic
Gas Tungsten Arc Welding (GTAW)

- B. Manual
Shield Metal Arc Welding (SMAW)

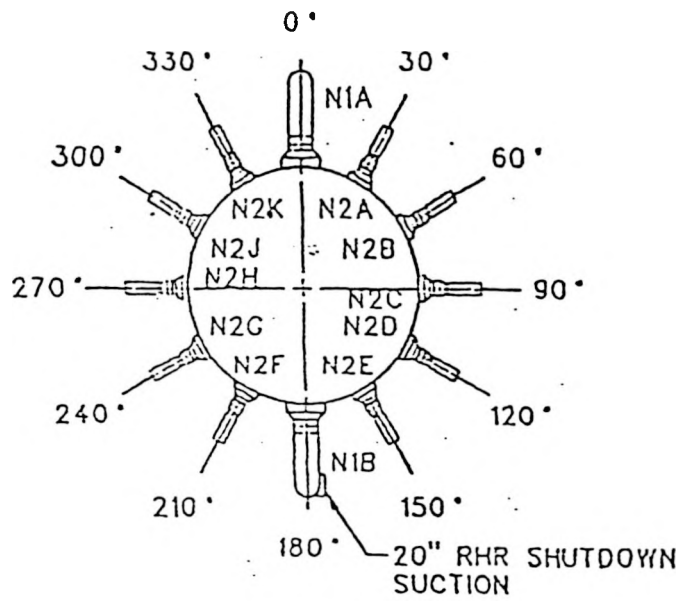
Radiation Exposure Control

- Performed in Accordance with the ALARA Program Provide by GE and Approved by Taipower

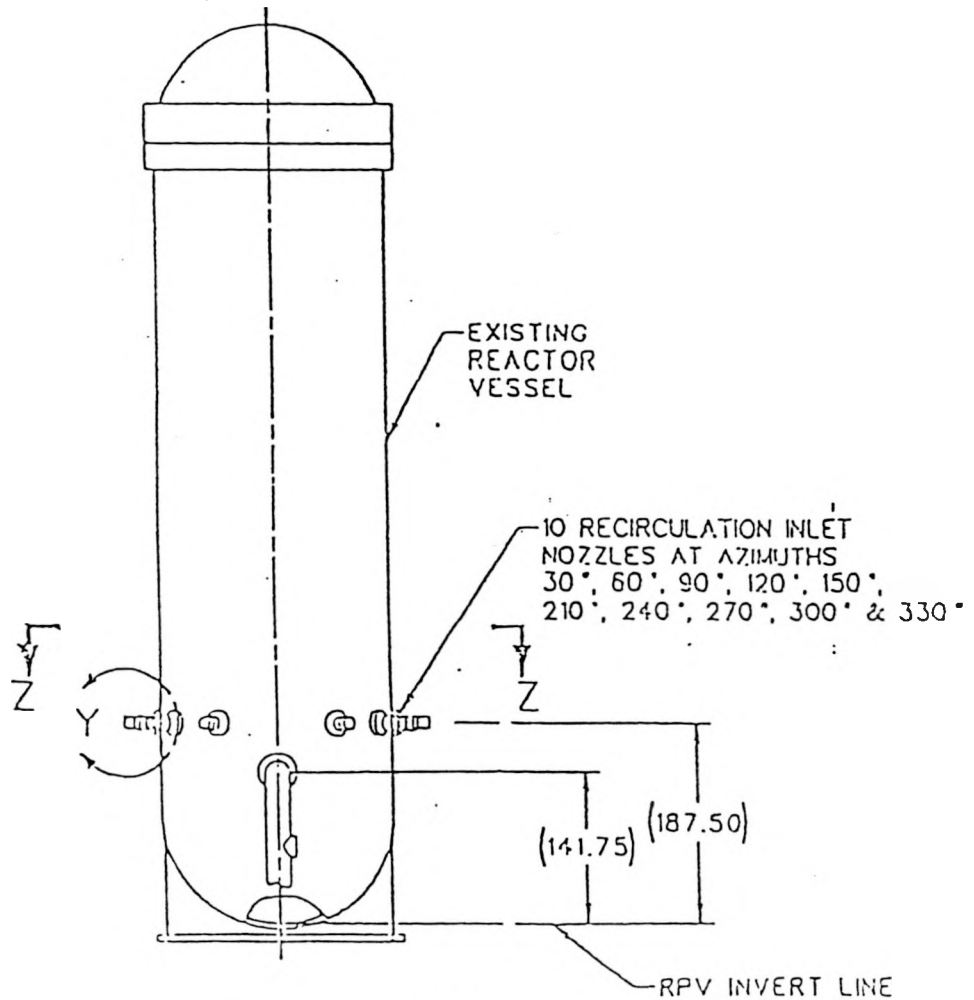
- Total Radiation Exposure
 - For Unit-2 : Predicted - 690 Man-Rem
 - Actual - 755 Man-Rem

 - For Unit-1 : Target - 590 Man-Rem
 - Actual - 184 Man-Rem
 - (Till 4/27/89)

Safe-End Replacement



SECTION Z-Z



1 MAKE FROM 15588527G002 (CHINSHAN 1)

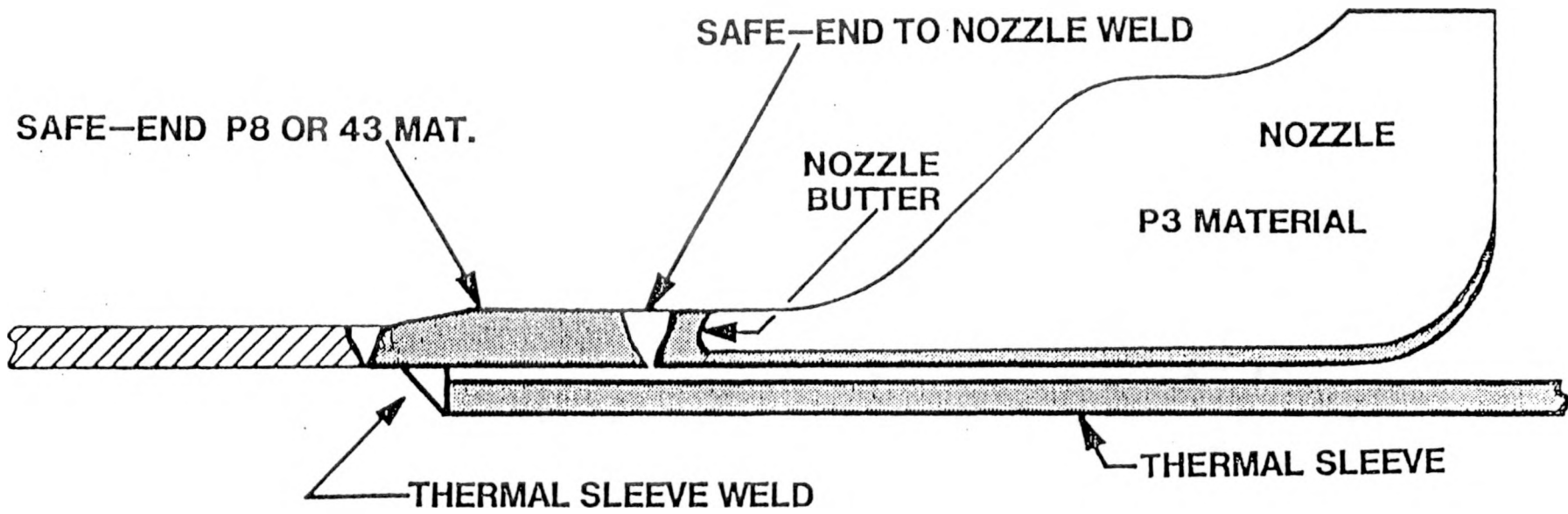
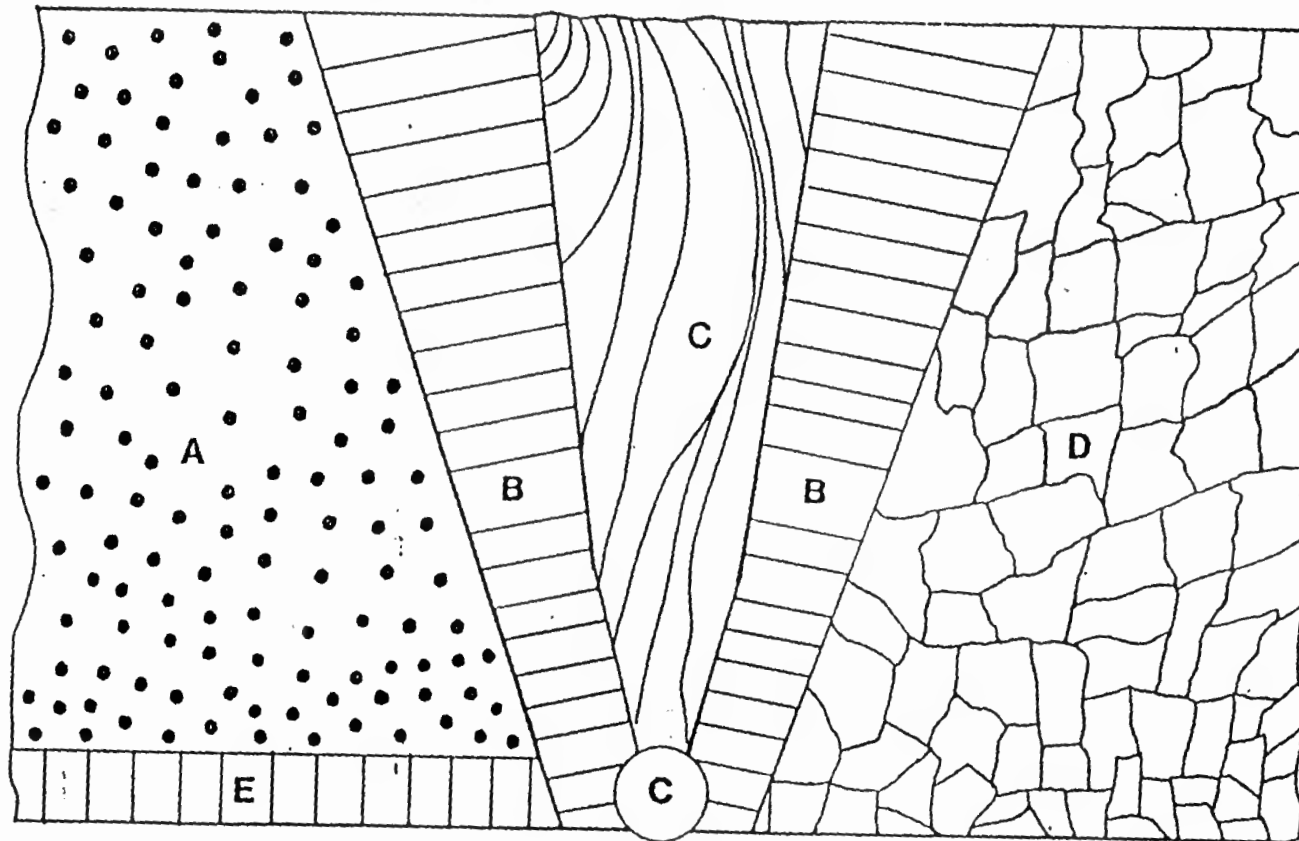


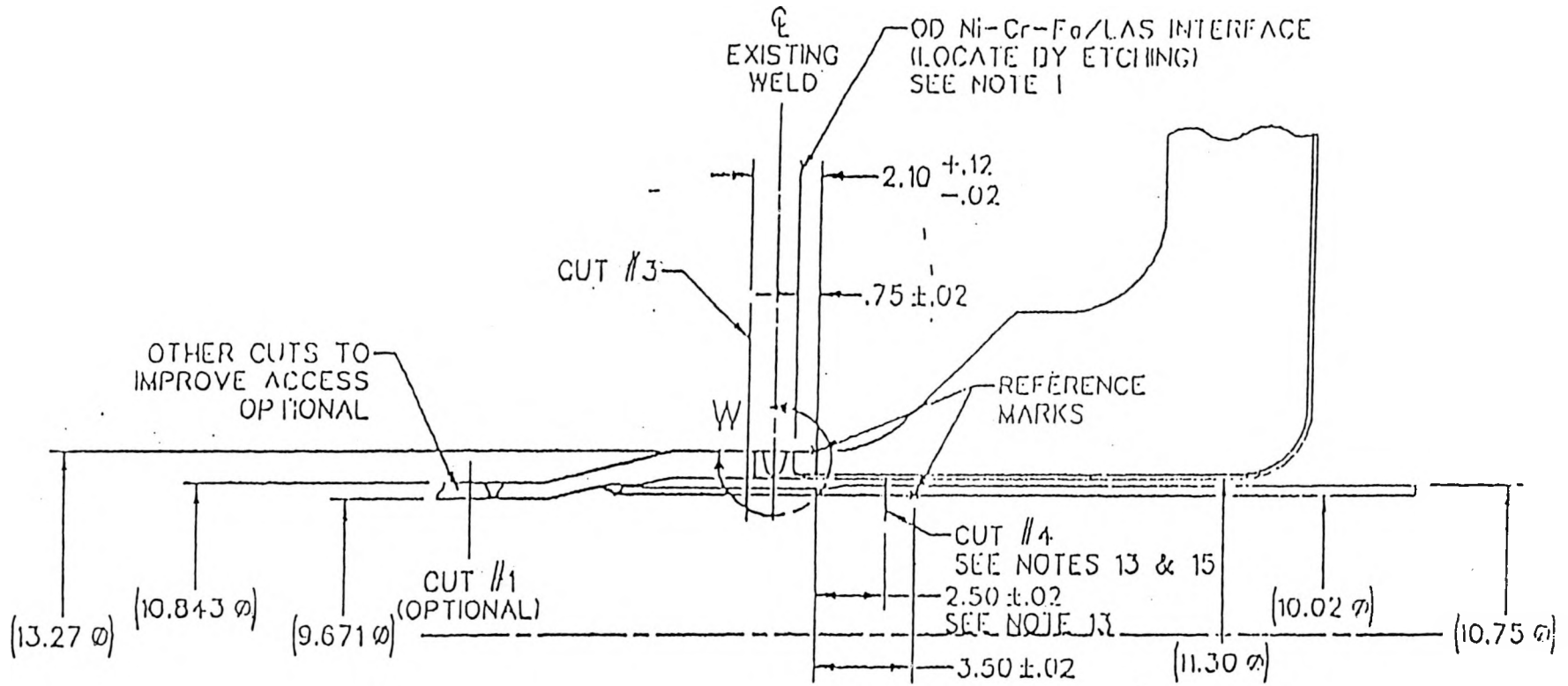
Figure 1. Typical Nozzle and Safe-End Configuration



- A - Low Alloy Steel Nozzle
- B - Alloy 182 Weld Overlay
- C - Alloy 182 or Alloy 82 Weld Metal
- D - Stainless Steel
- E - Stainless Steel Cladding

Figure 2. Safe-End Butter Configuration

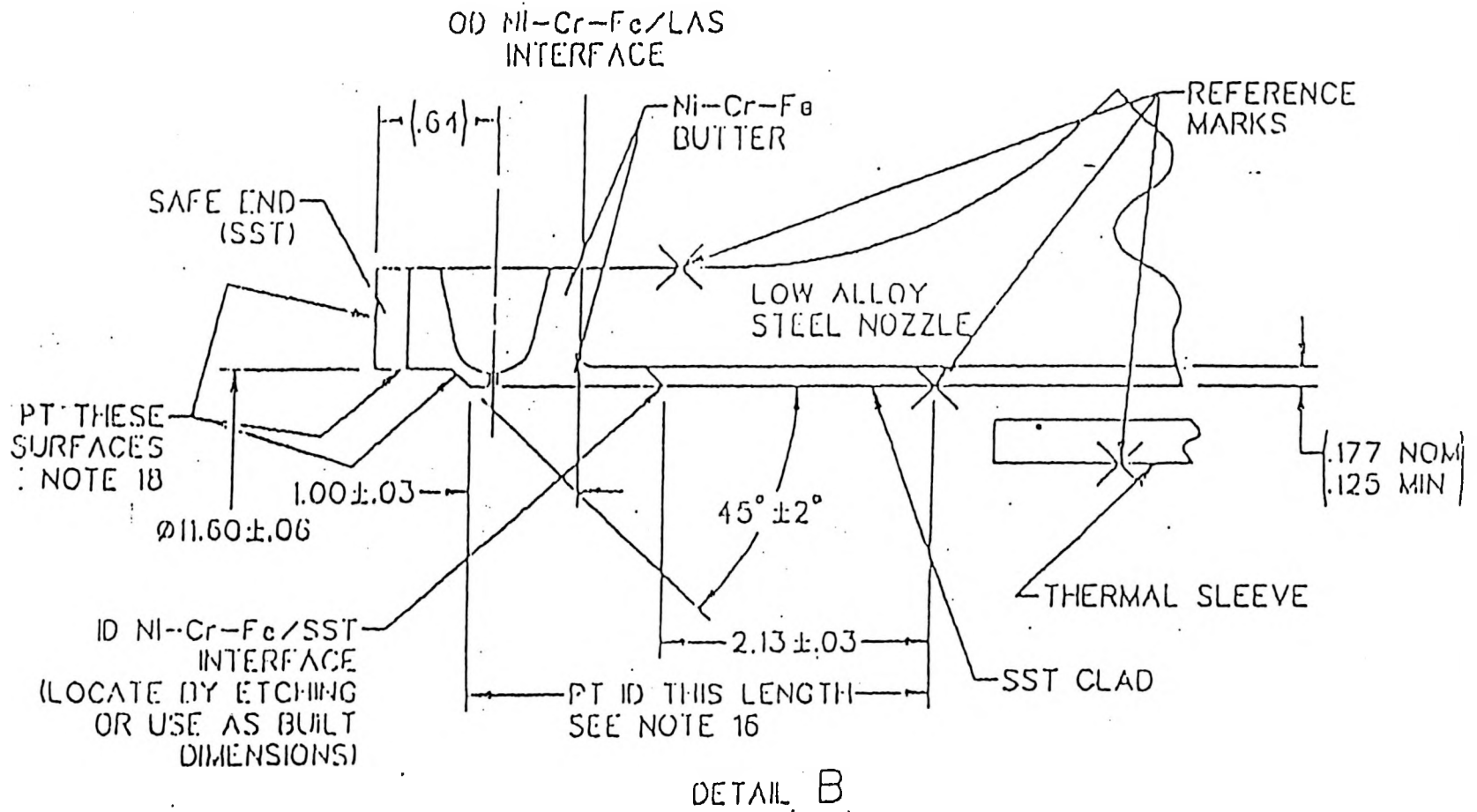
- STEP 1 LOCATE OD Ni-Cr-Fe/LOW ALLOY STEEL (LAS) INTERFACES,
ESTABLISH REFERENCE MARKS AND MAKE CUTS NEEDED FOR ACCESS
- STEP 2 ORIGINAL SAFE END MATERIAL REMOVAL AND ID MACHINING
- STEP 3 REMOVAL OF INDICATIONS
- STEP 4 THERMAL SLEEVE WELD PREP
- STEP 5 NOZZLE ID. WELD OVERLAY
- STEP 6 CUT #6 AND INITIAL NOZZLE WELD PREP MACHINING
- STEP 7 COMPLETION OF NOZZLE WELD PREP MACHINING
- STEP 8 TRANSITION PIECE WELD PREP
- STEP 9 FINAL ASSEMBLY



DETAIL Y

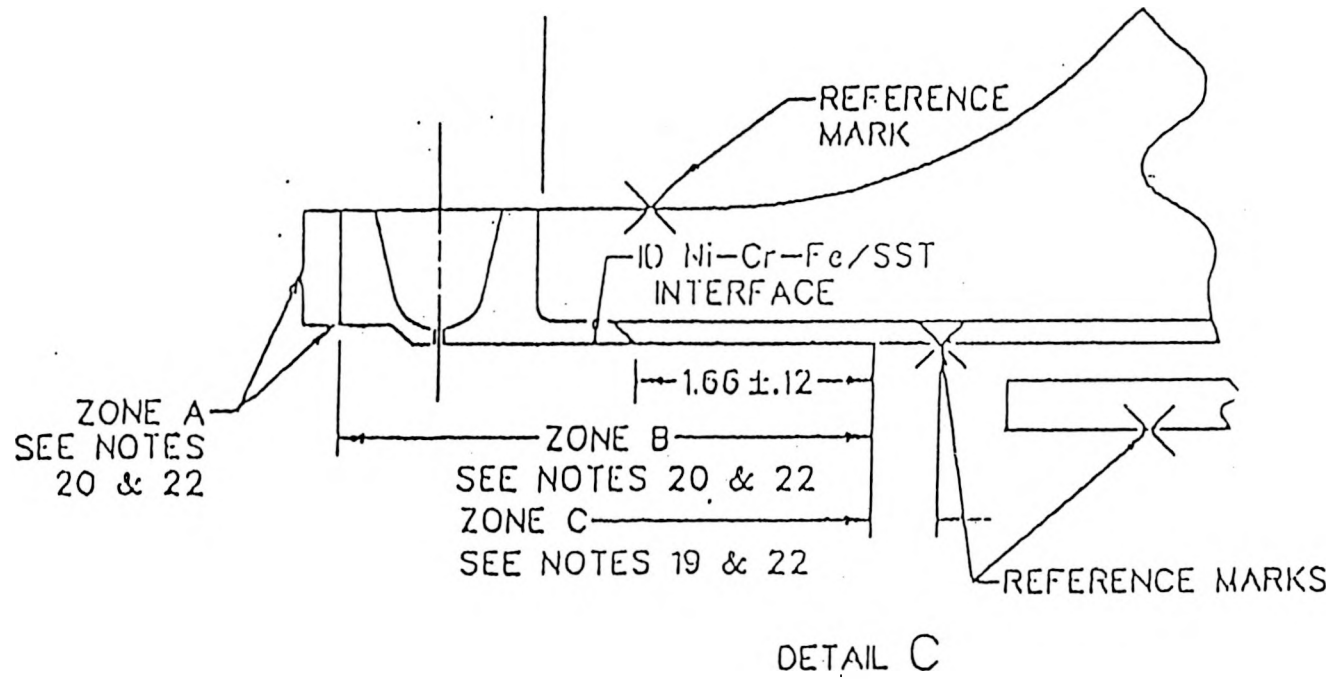
STEP 1 CHINSHAN 1 EXISTING NOZZLE, SAFE END AND PIPE SHOWING CUTTING PROCEDURES, ESTABLISHMENT OF REFERENCE MARKS AND LOCATION OF Ni-Cr-Fo/LAS OD INTERFACE

STEP 2 SEE DETAIL B



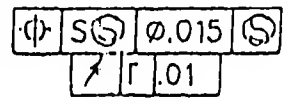
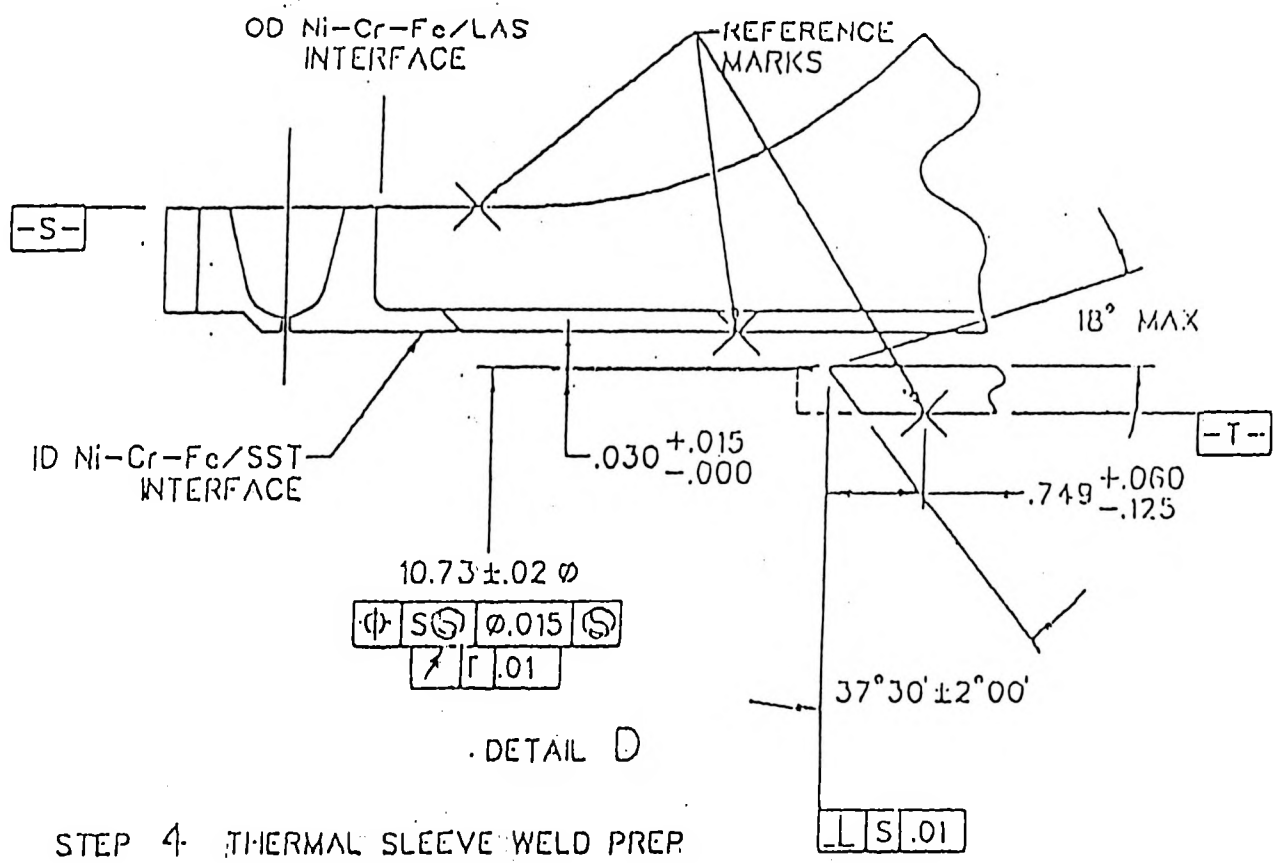
STEP 2 ORIGINAL SAFE-END MATERIAL REMOVAL MACHINING & ID INTERFACE

STEP 3 SEE DETAIL C

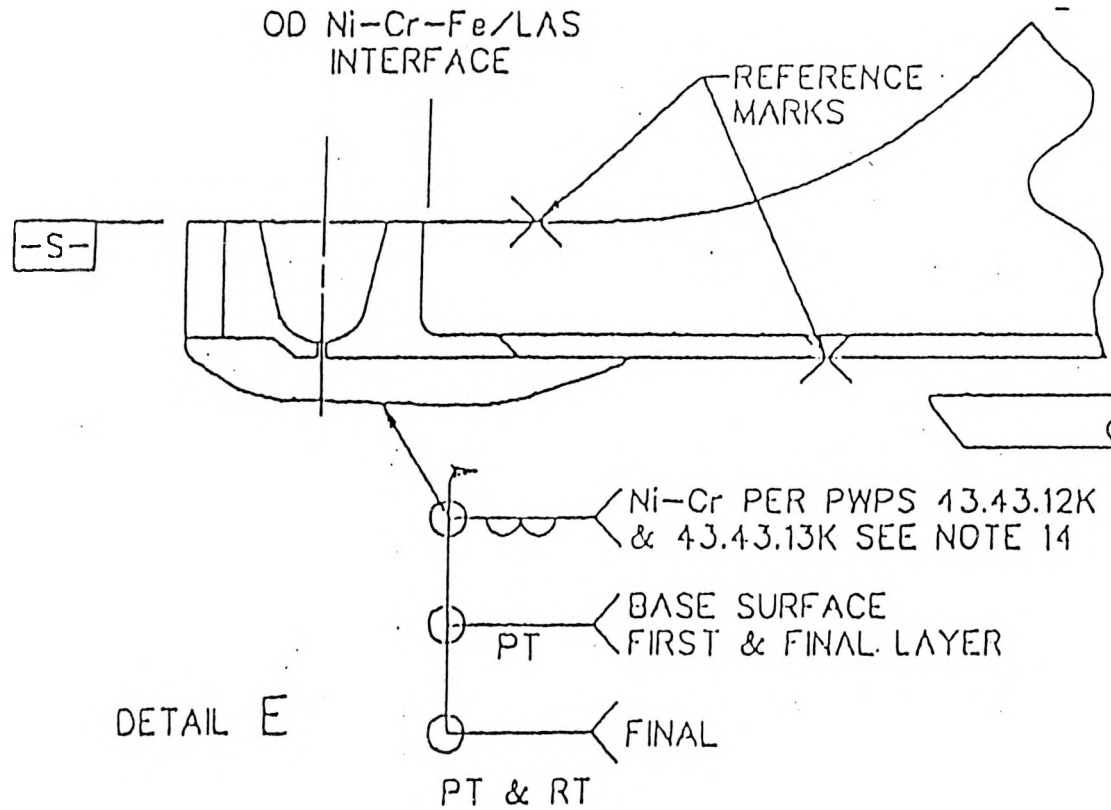


STEP 3 (REMOVAL OF INDICATIONS)

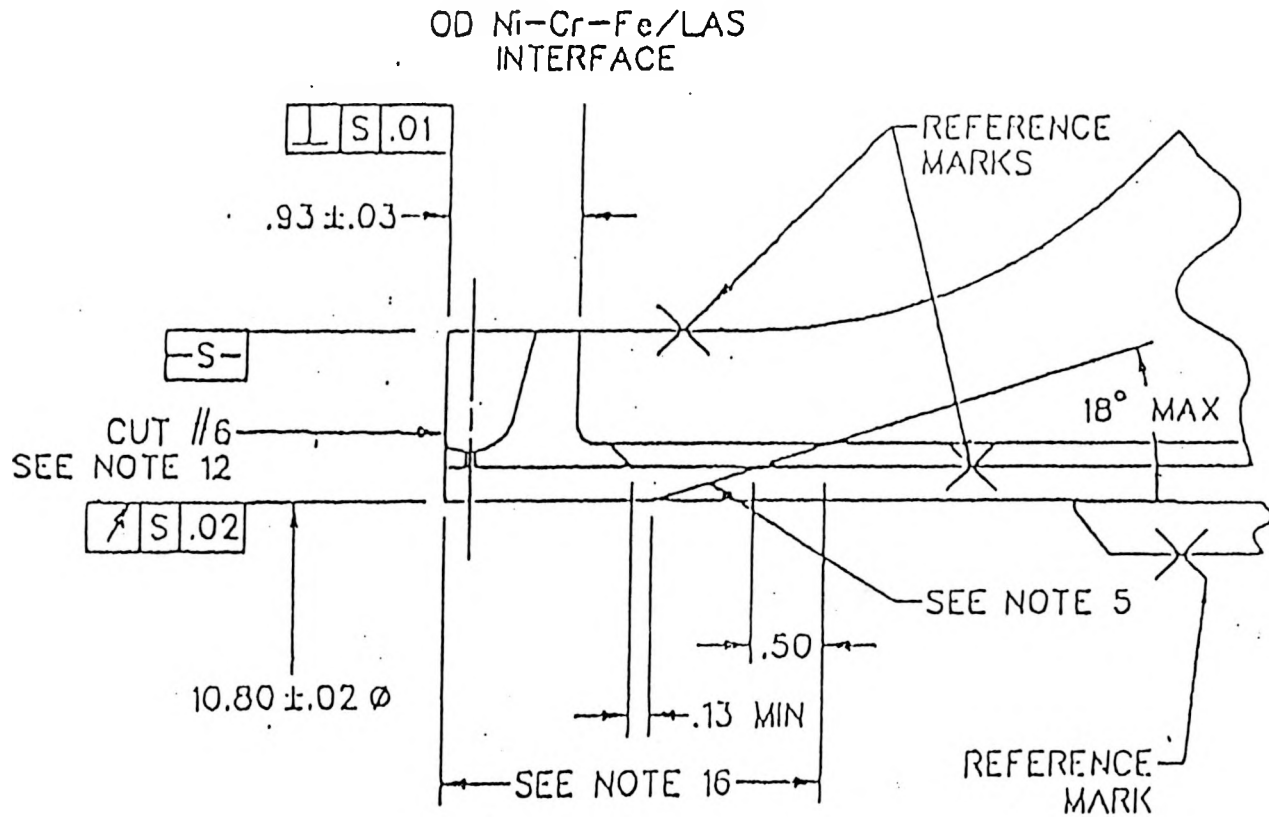
STEP 4 SEE DETAIL D



STEP 4: THERMAL SLEEVE WELD PREP
STEP 5: SEE DETAIL E



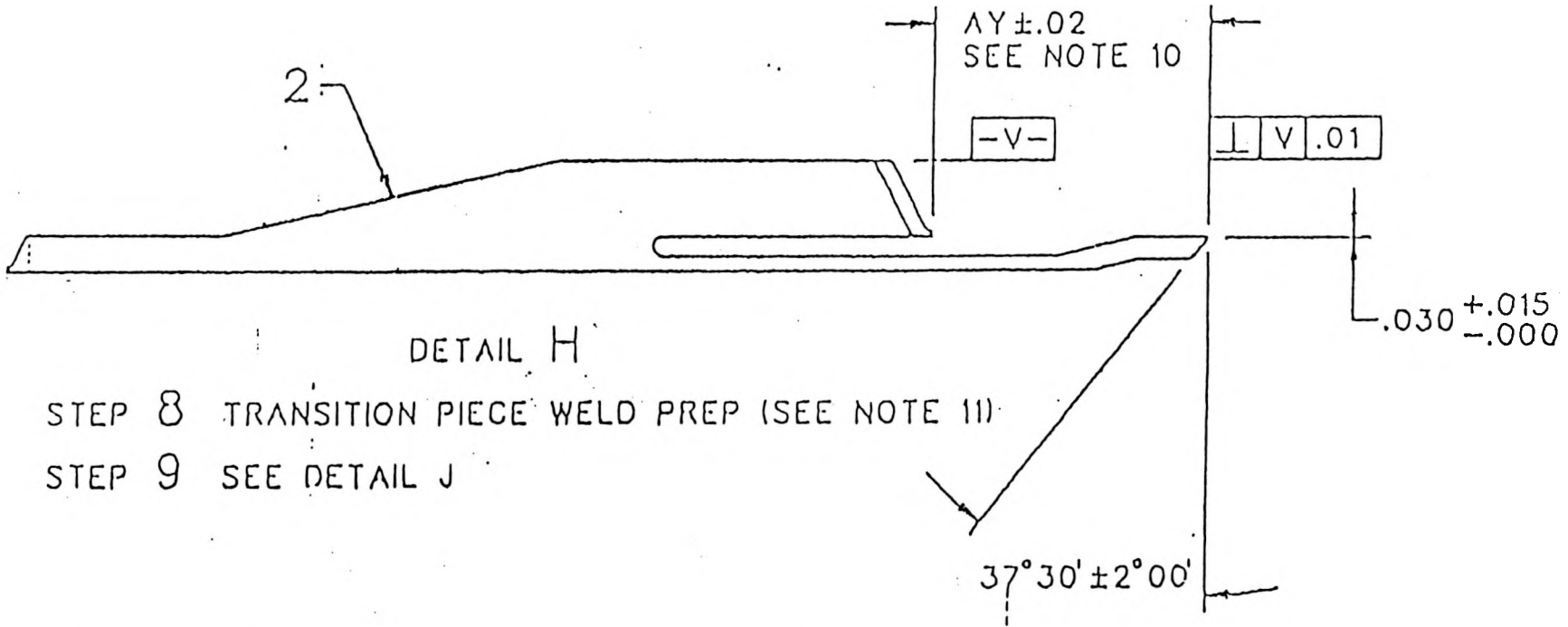
STEP 5 NOZZLE ID WELD OVERLAY *material?*
STEP 6 SEE DETAIL F *incom 82*



DETAIL F

- STEP 6 : CUT //6 AND INITIAL NOZZLE WELD PREP MACHINING
- STEP 7 SEE DETAIL G

324

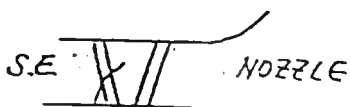






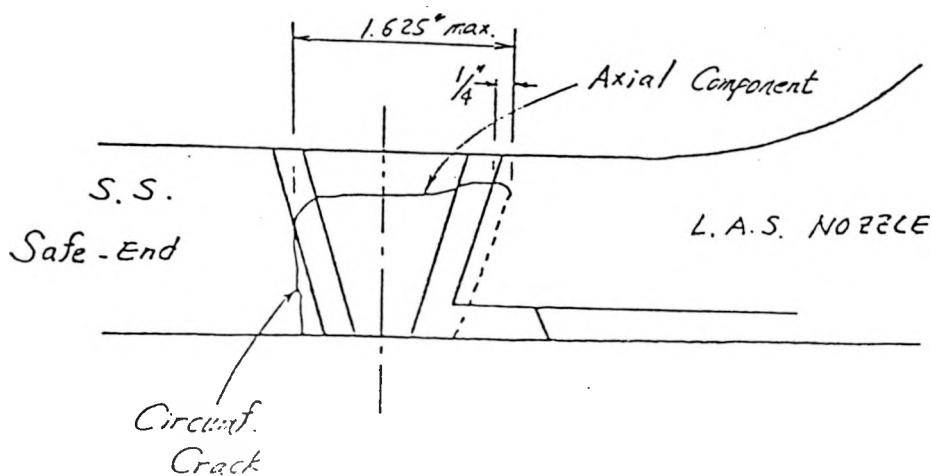
DETAIL H

- STEP 8 TRANSITION PIECE WELD PREP (SEE NOTE 11)
- STEP 9 SEE DETAIL J

Repair of N2E-F1 Weld (Unit#2 Only)

- Inspection Results During EOC-7

Time	By	Method	Results
1st	GE	AUT	Indication
2nd	GE	MUT	
3rd	GE	MUT	
4th	EBASCO	MUT	
5th	GE & E	MUT	
6th	TPC	RT	
7th	TPC GE EBASCO	RT UT UT	



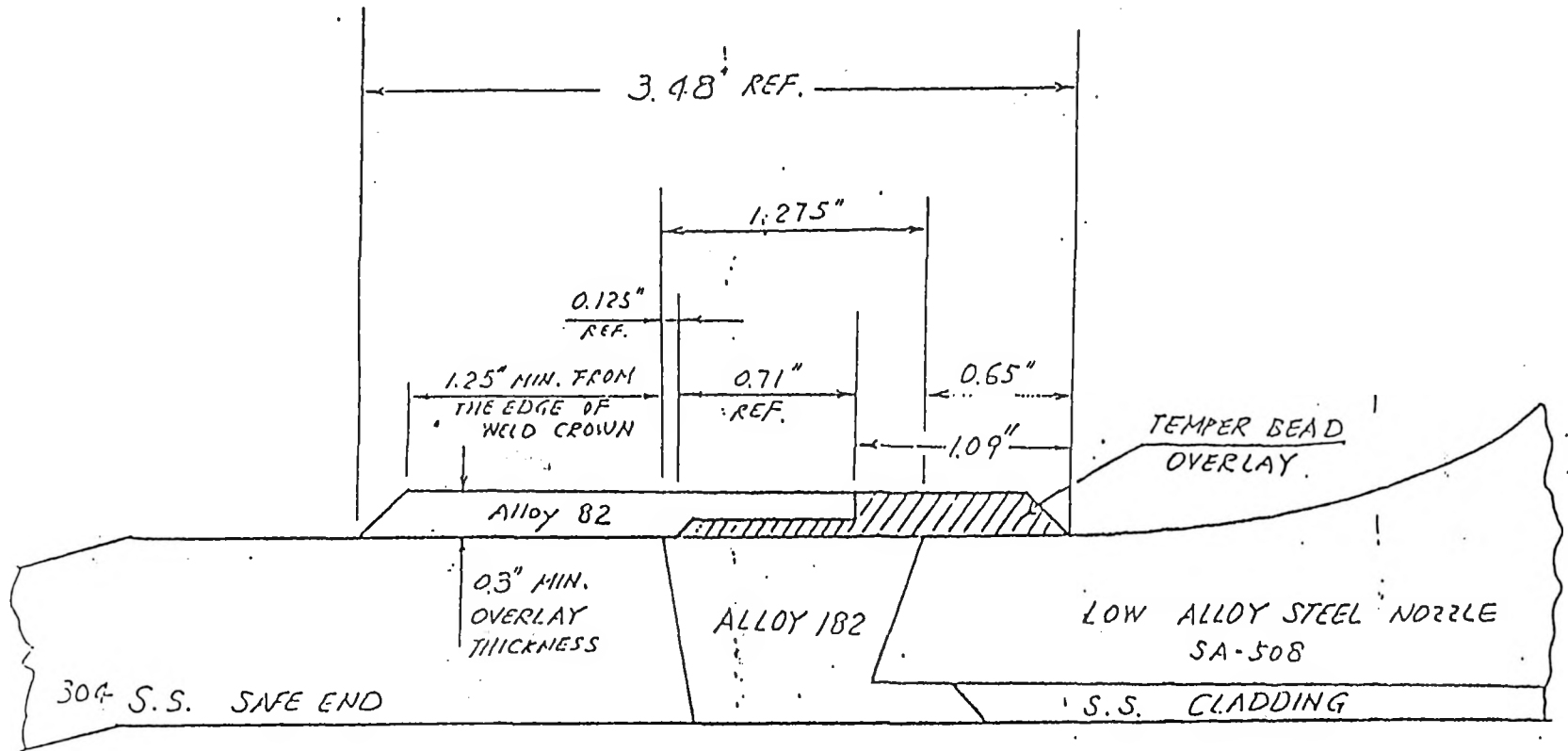
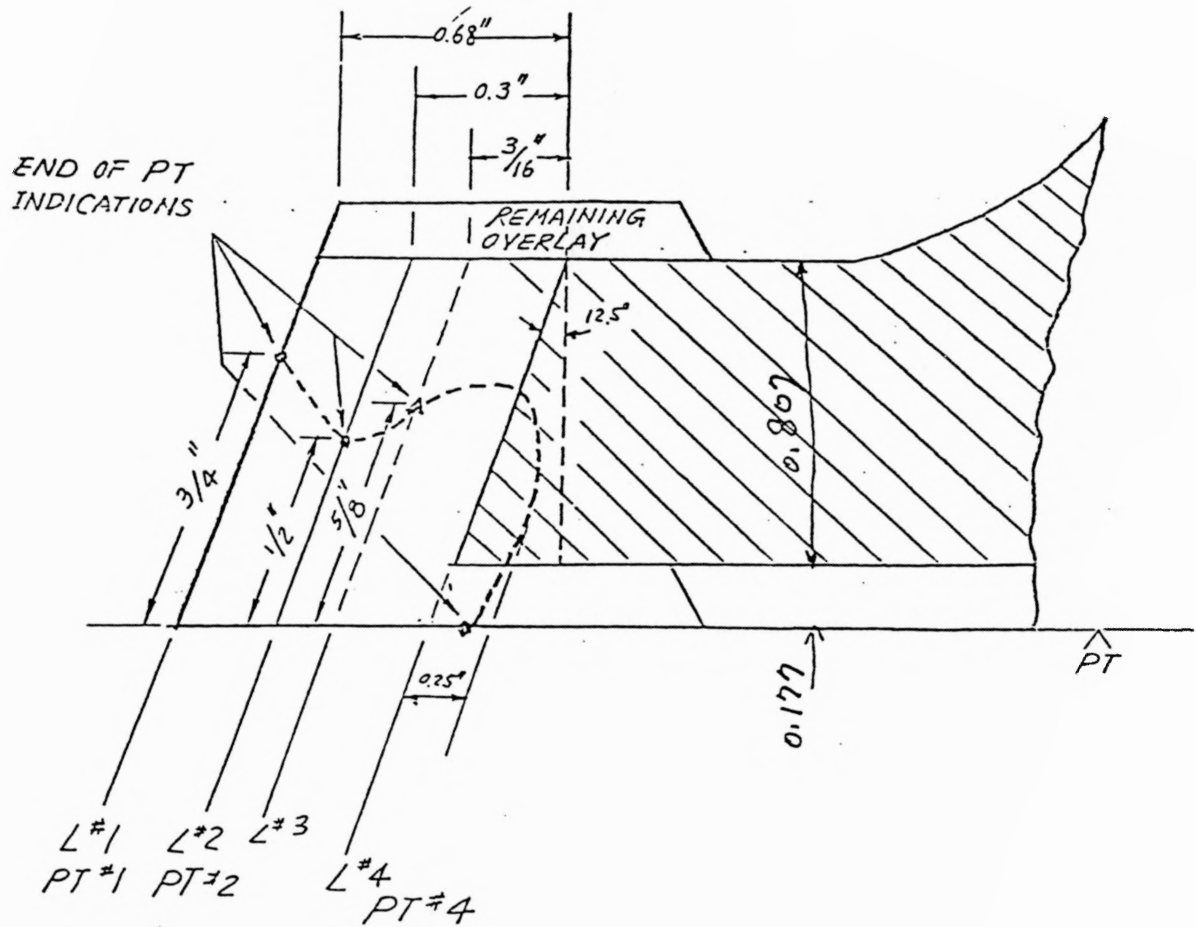


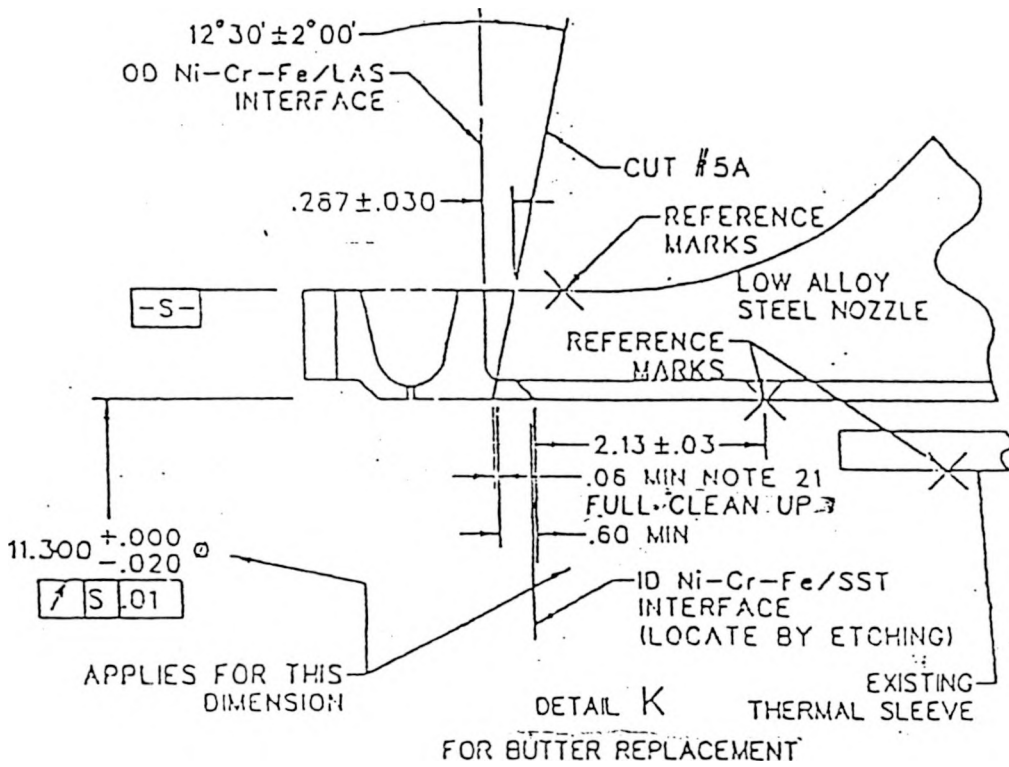
FIG. 3.1 WELD OVERLAY FOR N2E-F1

- N2E-F1 Weld Inspection Results (EOC-8)



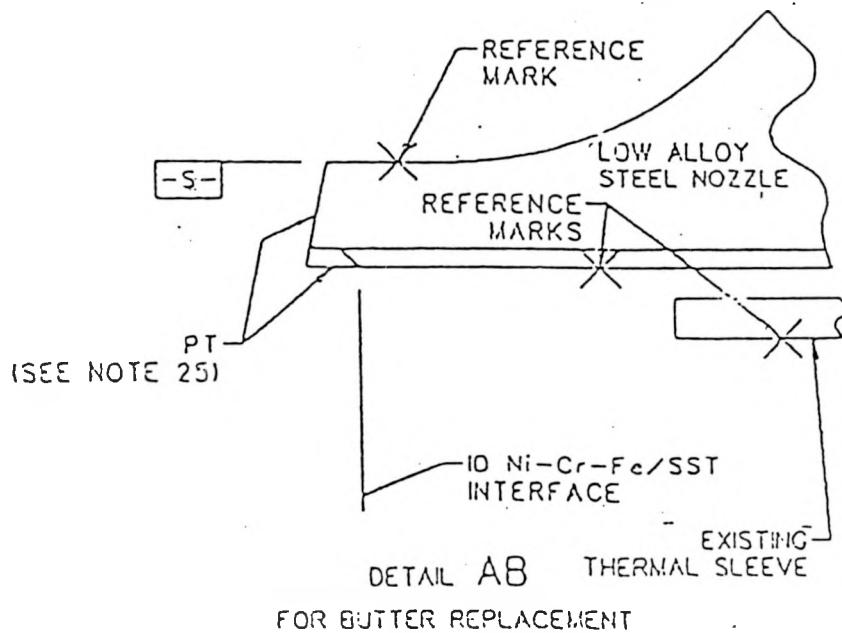
N2E PT RESULTS 12/16/88

N2E-F1 Safe-End Replacement



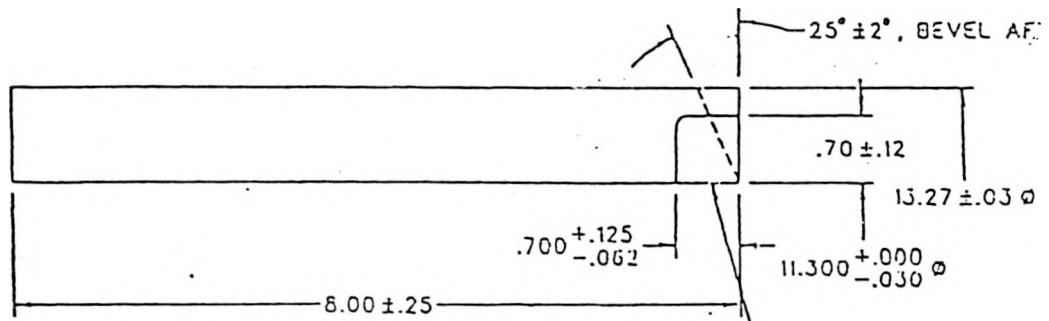
STEP 2A CUT #5A

STEP 2B SEE DETAIL AB

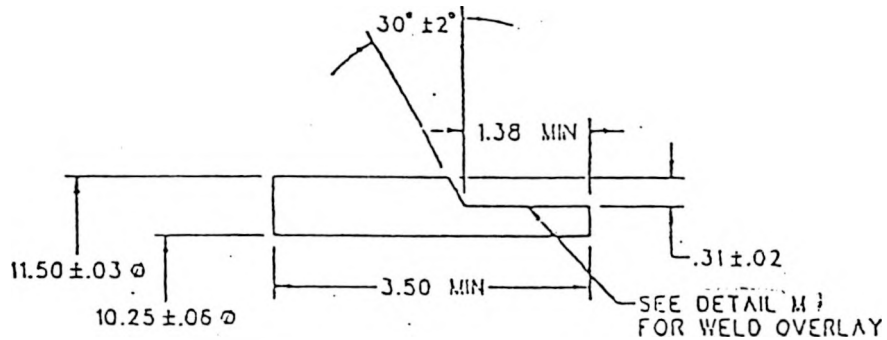


STEP 2B LOW ALLOY STEEL CRACK REMOVAL AND REPAIR, IF REQUIRED

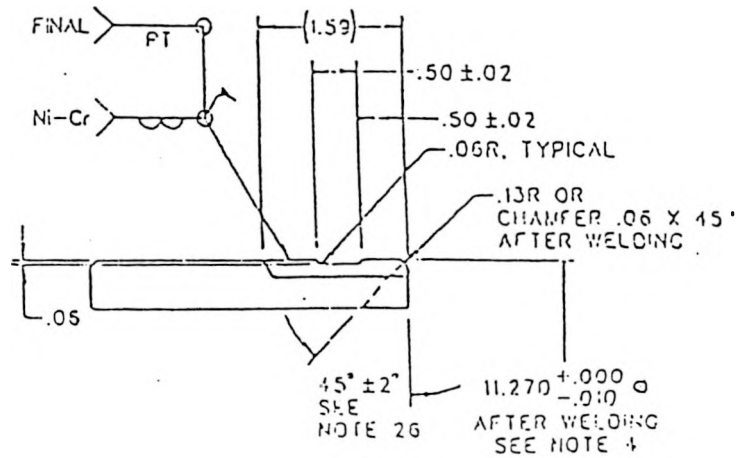
STEP 2C SEE DETAIL L



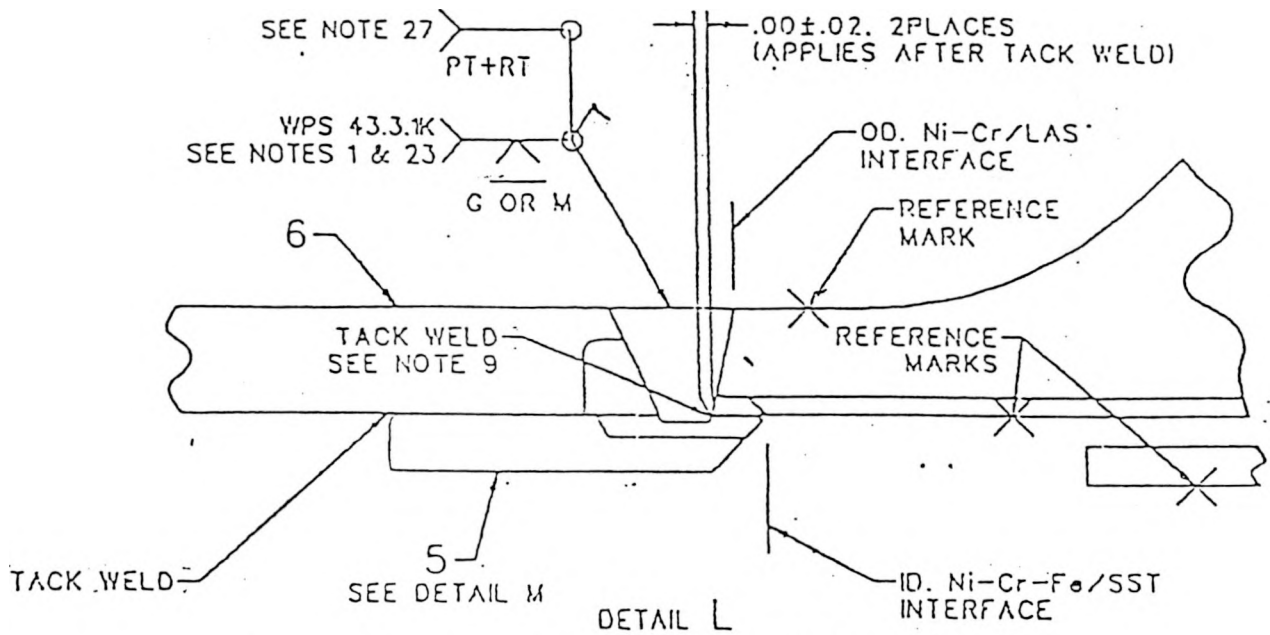
6 SACRIFICIAL RING
 MATL: 304 OR 304L STAINLESS STEEL,
 CARBON OR LOW ALLOY STEEL,
 OR Ni-Cr-Fe



5 BACKING RING
 MATL: 304 OR 304L STAINLESS STEEL,
 CARBON OR LOW ALLOY STEEL,
 OR Ni-Cr-Fe



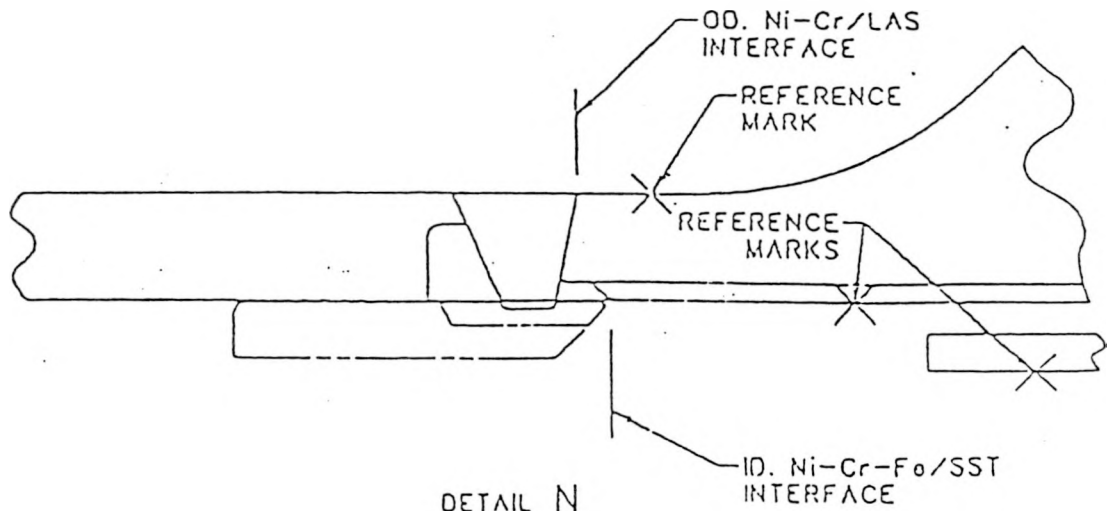
DETAIL M
 FOR BUTER REPLACEMENT



FOR BUTTER REPLACEMENT

STEP 2C INSTALL BACKING RING AND SACRIFICIAL RING. APPLY BUTTER

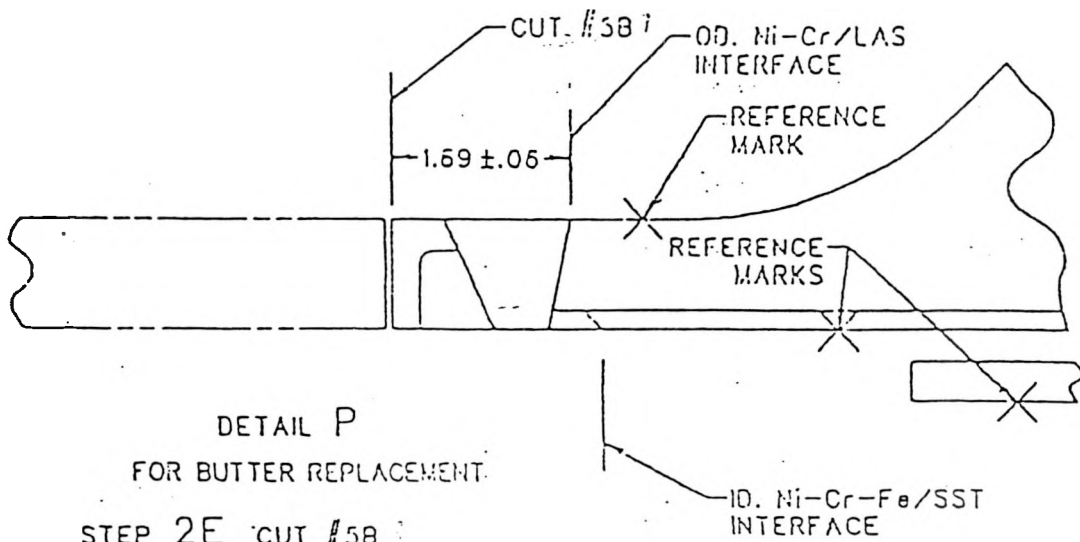
STEP 2D SEE DETAIL N



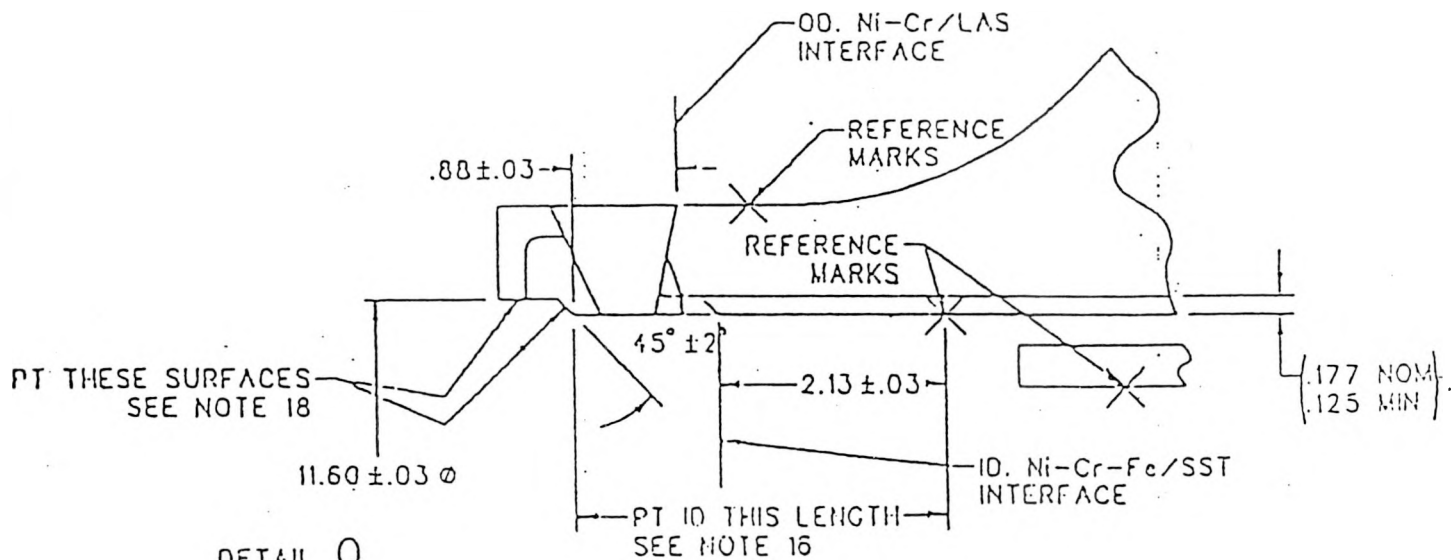
FOR BUTTER REPLACEMENT

STEP 2D REMOVE BACKING RING

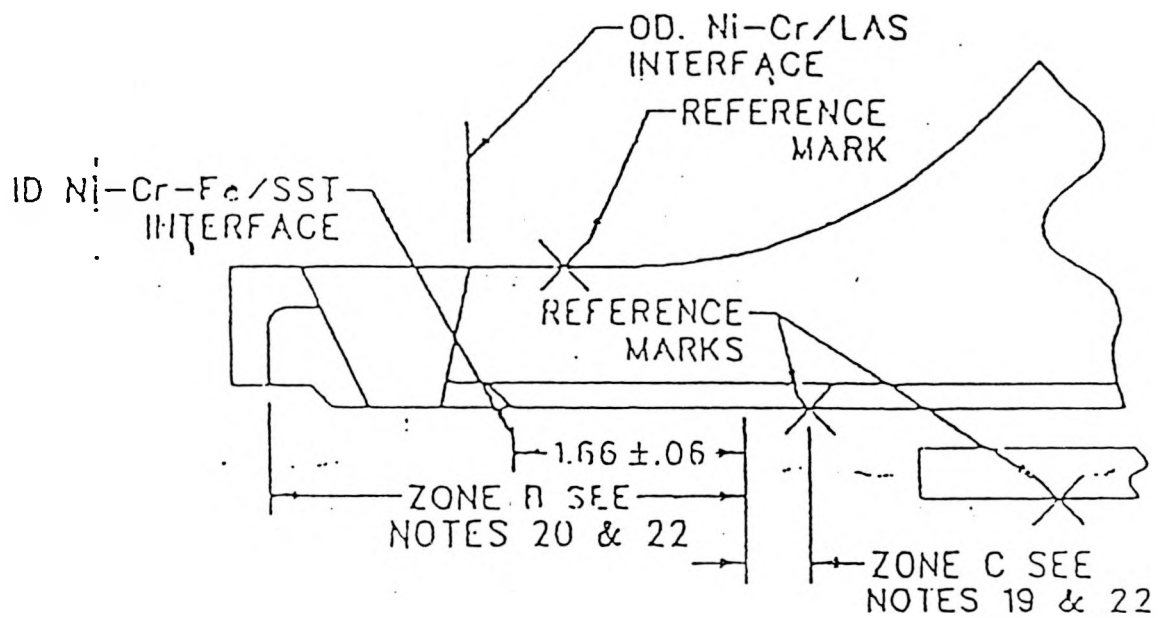
STEP 2E SEE DETAIL P



DETAIL P
 FOR BUTTER REPLACEMENT
 STEP 2E CUT #58
 STEP 2F SEE DETAIL O



DETAIL Q
 FOR BUTTER REPLACEMENT
 STEP 2F MACHINING AND INDICATION PT
 STEP 3A SEE DETAIL R



DETAIL R

FOR CHINSHAN 2 NOZZLE N2E ONLY

STEP 3A REMOVAL OF INDICATIONS

STEP 4 SEE DETAIL D (SHEET 1 ZONE D-9)

Major Problem and Resolve Methods

Major Problem

1. High Welding Failure Rate

Unit#2 - 15 of 60 (25%)

Unit#1 - 1 of 14 (<5%) Till 4/27/89

2. Schedule Was Extended From 35 Days to 88 Days for Unit #2 Replacement Work

*Causes:

In Personnel:

- Insufficient Training
- Poor Schedule Control
- Poor Engineering Staff Support

In Equipment:

- Radiation Shielding
- Tool Design Need Improvement
- Number of Tools

In Work Procedure:

- Too Much Revise During Work Were Proceeding
- Too Many Nonconformance Reports Issued

Resolve Methods

- Improve Work Training
 - . Project Scheduling
 - . QC
 - . ALARA HP
 - . Full Size Mock up
- Improve Organizational Management
 - . Reinforce Project Engineering Group
 - Problem Solving
 - . Establish Production Group
 - Progress Control
 - . Add Shift Engineer, ALARA Technician and QC Inspectors
- Improve Radioactive Shielding
- Review All Working Procedures Thoroughly Before Work
- Improve Progress Control
 - . Install Site Monitor
 - . Issue Understandable Schedule
 - . Daily Meeting

APPENDIX A
LIST OF ATTENDEES

IPIRG TAG MEETING
MAY 8--10, 1989

LIST OF ATTENDEES

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IPIRG TAG MEETING
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Mr. H. L. Chang	U. of Tsing-Hwa	101, Sec. 2, Kung-Fu Road. Hsinchu, Taiwan, R. O. C.

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December	1989					
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12 SUPPLEMENTARY NOTES		11a TYPE OF REPORT				
13 ABSTRACT (200 words or less)		Conference Proceedings				
<p>The fourth in a series of international Leak-Before-Break (LBB) Seminars supported in part by the U.S. Nuclear Regulatory Commission was held at the National Central Library in Taipei, Taiwan on May 11 and 12, 1989. The seminar updated the international policies and supporting research on LBB. Attendees included representatives from regulatory agencies, electric utility representatives, nuclear power plant fabricators, research organizations, and academic institutions.</p> <p>Regulatory policy was the subject of presentations by Mr. G. Arlotto (U.S. NRC, U.S.A.), Dr. B. Jarman (AECB, Canada), Dr. P. Milella (ENEA-DISP, Italy), Dr. C. Faldy (EDF/Septen, France), and Dr. K. Takumi (NUPEC, Japan). A paper by Mr. K. Wichman and Mr. S. Lee of the U.S. NRC Office of Nuclear Reactor Regulation is included as background material to these proceedings; it discusses the history and status of LBB applications in U.S. nuclear power plants. In addition, several papers on the supporting research programs described regulatory policy or industry standards for flaw evaluations, e.g., the ASME Section XI code procedures. Supporting research programs were reviewed on the first and second day by several participants from Taiwan, U.S., Japan, Canada, Italy, and France.</p>		b PERIOD COVERED (Inclusive dates)				
May 11-12, 1989						
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